STANFORD LINEAR ACCELERATOR CENTER (A)

Development of a Remotely Operable High-Vacuum Coupling

In September 1963, Leo Bloom*, an engineer in the Mechanical Component Design Group at the Stanford Linear Accelerator Center (SLAC) near Palo Alto, California began design work on a remotely operable high-vacuum coupling. The coupling was for use in the beam switchyard of the accelerator, where instruments and control magnets which must be quickly replaceable are located. The couplings were needed in three sizes, with I.D.'s of 6 inches, 10 inches, and 12 inches. It had to be possible to connect and disconnect them from manholes up to 10 feet distant using hand tools.

The SLAC linear accelerator is the world's largest; it cost $114 million to build and develops 20 Bev to bring electrons to speeds 99.999999% that of light. The SLAC project was undertaken by Stanford University for the U.S. Atomic Energy Commission. Preliminary operation began in 1966, with full operation expected by 1967, after which a staff of over 1,000 would be retained. In 1966 there were about 40 mechanical engineers at SLAC, with about twice that many electrical engineers.

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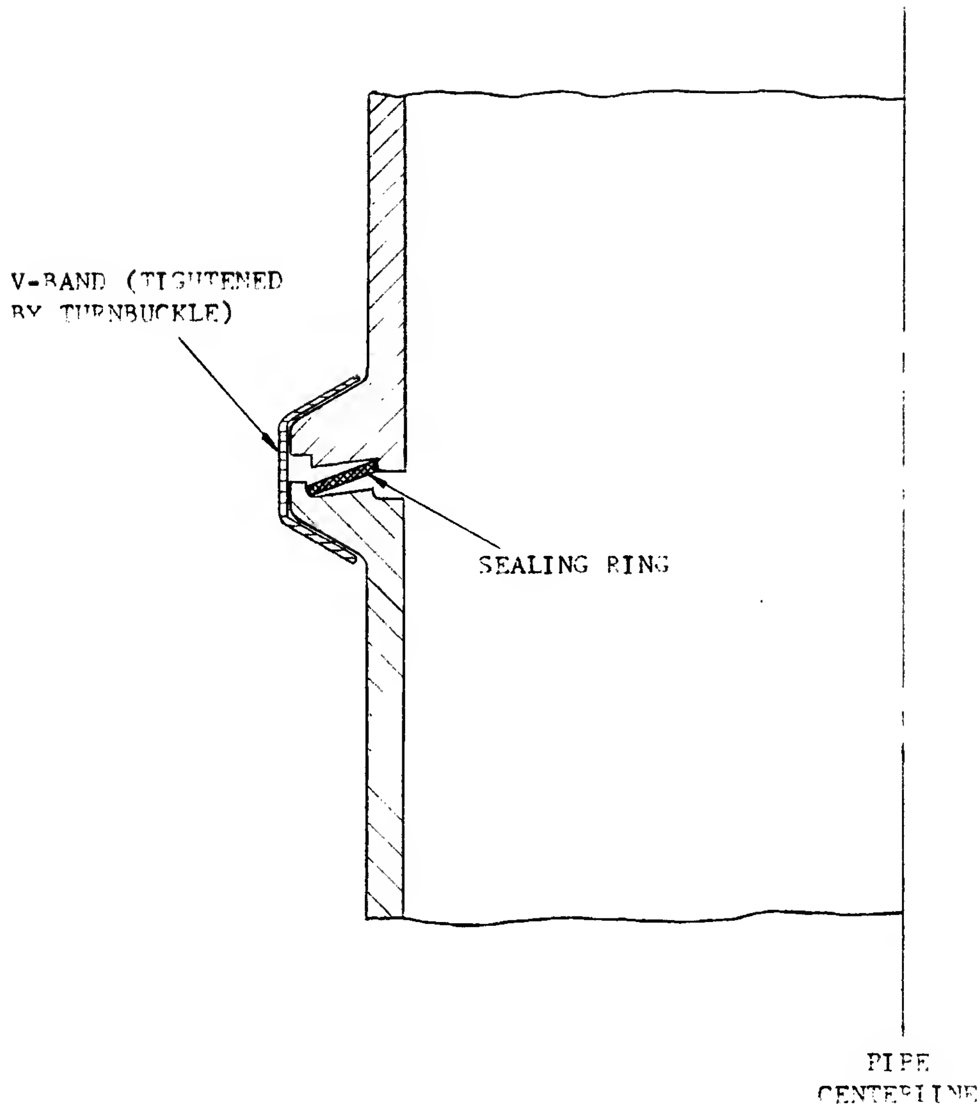
* Fictionalized name.

Prepared in the Design Division of the Department of Mechanical Engineering by John A. Alic with financial support from the National Science Foundation.



<u>Exhibit 1:</u> Commercially Available All-Metal Coupling Tested by SLAC.

After a 10,000 foot straight run, the electron beam enters the switchyard, where it may follow one of the three diverging branches. More than 200 couplings were needed in this area. The radiation intensity level in the switchyard varies greatly with location but a typical value might be about 10^{11} rads* for a 10 year period. Elastomers become hard and brittle when exposed to more than about 10^9 rads and even inert substances like teflon deteriorate in the radiation environment, so from the beginning the engineers at SLAC planned to use all-metal seals with the vacuum couplings. It is the radiation hazard that makes remote assembly and disassembly of the couplings necessary.

In March 1964, a committee of engineers and physicists concerned with many different phases of the accelerator project set design criteria for the couplings. At this time a maximum allowable leakage rate of 10^{-4} cc/sec per joint was decided upon. The vacuum in the switchyard is about 10^{-5} Torr.** In addition to those previously mentioned, the criteria included minimum coupling size, particularly axial length, although no numerical limits were set at this time.

Before the design criteria had been formally established, Leo Bloom had been investigating commercially available couplings. He found only one that seemed as if it might be suitable. A sketch of this coupling appears in Exhibit 1. The re-useable seal is a conical ring of copper, beryllium copper or stainless steel which is clamped between two parallel faces by the action of the V-band. The V-band is tightened circumferentially by a single turnbuckle and pulls the two flanges together.

A testing program was planned to evaluate this coupling, but it was felt that SLAC should begin development of an in-house design also. Leo had available to him a considerable backlog of indium seal experience. Indium is often used for high vacuum seals -- most often simply as a wire gasket between two flat, parallel flanges which are bolted together -- because it is highly compliant; this property minimizes sealing problems caused by poor surface finish, dirt and grit, or flanges which are not perfectly flat. Since indium has a yield strength of only 400 psi, such seals need relatively low clamping forces, which Leo could see as an important advantage in a coupling designed for remote manipulation. A disadvantage of indium for some applications is its low melting point -- 311°F -- which limits its use to systems which do not require baking out. Some high vacuum systems are heated to high temperatures to drive out absorbed gases as well as moisture and other contaminants. This is only necessary for a vacuum harder than that in the SLAC accelerator, however.

Leo began thinking about ways he could use an indium seal in a remotely operable coupling.

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* The rad is a unit of absorbed radiation. One rad equals an energy absorption of 100 ergs per gram of material.

** One Torr equals one mm Hg.

STANFORD LINEAR ACCELERATOR CENTER (B)

Development of a Remotely Operable High-Vacuum Coupling

The SLAC Design

By March 1964 Leo Bloom had found that the commercial coupling described in Part A would not be completely satisfactory. He said, "Our tests showed that sometimes it would perform well, but at other times a lot of fiddling and adjustment was needed to make it seal at all. Much of this trouble stemmed from the fact that the coupling was very sensitive to dirt and grit. Even a single very small particle could ruin the seal, since the imbeddability of the sealing ring is poor. It seemed that impractically rigid quality control by the manufacturer might be needed to get less erratic behavior. I also felt that the V-band tightening mechanism might not give a uniform axial force with the larger couplings because of frictional effects in the large surface areas involved."

In July a 6 inch indium seal coupling of Leo's design went on test and by January 1965 designs for all sizes were complete. Photographs of a coupling appear in Exhibits 1 through 4. Soon after the design was completed, Russ Miksch took over the project. A SLAC technical note which Russ prepared later to document the coupling development experience appears as Exhibit 5. Included are the design criteria which had been decided upon in March 1964, and test results. An assembly drawing of the female half of a 12 inch coupling is shown in Exhibit 6 and the male half is shown in Exhibit 7. In Exhibit 6, item 1 is the female flange and item 6 is the ring-spring. Items 5 are retainers which hold the ring-spring in place during assembly. None are used on the male half. Item 15 is the cammed hook and item 4 its actuator. Items 10 are the load springs which transfer the force applied at the cammed hooks to the ring-springs. The guide pins, items 2 and 3 in Exhibit 6 together with the guides on the male half, items 4 and 5 in Exhibit 7, aid in the assembly of the coupling. Item 2 in Exhibit 7 is the ring-spring. The hooks connect to pins (item 7) between the ears of this ring-spring. Details of the male flange appear in Exhibit 8. A detail of the groove in the female flange for the indium is shown in Exhibit 9. A detail drawing for the ring-spring is shown in Exhibit 10. These drawings are all for 12 inch couplings.

Leo Bloom chose a knife edge seal because the couplings are most often used in the vertical position and a wire gasket could drop out too easily when the coupling was disconnected. Leo said, "The stepped knife edge is a compromise; we wanted the indium to be distorted and forced to flow plastically as much as possible during imbedding but we also wanted low force for imbedding. Low force is important because of the space constraints which limit the sizes of the ring-springs and load springs.

With the stepped knife edge, the metal deforms locally at each corner but the force needed is higher than it would be for a triangular knife edge. We used re-entrant sides on the groove, not only to keep the indium in place should adhesion occur, but also so more work would be done on the indium during imbedding, giving better conformance to the knife edge. We wanted as small a groove as practical, to minimize the cost of indium."

Indium of 99.999% purity is used because hardness increases greatly with only small amounts of impurities and the required force per lineal inch of seal for good imbedding could rapidly become excessive. In small quantities, indium of this purity costs $5 per troy ounce, however the price is lower for large purchases; SLAC bought more than 100 lbs at $3.50 per troy ounce. It is shipped to them in the form of strips held between two sheets of plywood.

Before deciding upon the cammed hook arrangement, with springs to load the ring-springs, Leo gave some consideration to using other kinds of toggles or screw jacks, but found they would all require more parts, sources of added cost as well as lost motion and backlash. The cammed hooks are commercially available assemblies; the idea for using such a mechanism came to Leo when he remembered seeing similar devices used to close coffins during World War II.

During manufacturing of the couplings the ring-springs, cammed hooks and load springs are assembled to the flanges and placed over a neck on the piece of equipment. The flange is then welded on from the inside. Russ Miksch said, "This minimizes 'virtual leaks' caused by gas trapped in the seam or in pockets which can leak into the system after welding if the weld is made on the outside of the flange."

The decision to use the ring-springs on the two larger couplings, while relying upon flange stiffness to distribute the load from the two points of application for the 6 inch coupling, was based on an intuitive judgement of the situation by Leo. No calculations were made, but he said, "Out early tests showed that a clamping force of 100 lbs per lineal inch of seal would give a high probability of a good seal. This was actually a pretty arbitrary number, but it gave us a target, with some assurance of good results. It then seemed to me that to get a force of this magnitude distributed equally around the larger flanges we'd have to apply force to the flanges at more than two points to avoid bowing. Since the remote manipulation requirement allowed us only two points of primary load application, we were led to the ring-springs to distribute this load around the flanges. We knew that the force per inch for sealing would vary depending on the penetration of the knife edge, but before designing the springs to apply the load, we had to have a ring-spring that would distribute it evenly."

First Leo had to decide at how many points the ring-spring should contact and apply load to the flange. He thought it obvious that two of the points of load application should be beneath the ears of the ring, in line with the loads applied by the cammed hooks and load springs. Leo saw that if he placed raised bosses at these two locations, and another set of two bosses, raised by some greater amount, at 90° from the ears,

he would have a simple and manageable deflection curve. He said, "Likewise, with bosses beneath the ears, and four more at 60° from the ears, we still had a situation where, although there are six forces applied to the rings, the four at 60° from the ears are all equal.* I thought we could get equal forces at all six points using this configuration without too much trouble. While we wanted to apply force to the flange at as many points as possible, to minimize both flange deflection and ring stiffness, I could see that, if we went to eight points of force application, every 45°, the situation would become too indeterminate and we would have to be very careful, both in designing and manufacturing the ring, to get equal loads. Even then, in actual practice it might be impossible."

Leo's next problem was to analyze the ring to see what section properties he would need.

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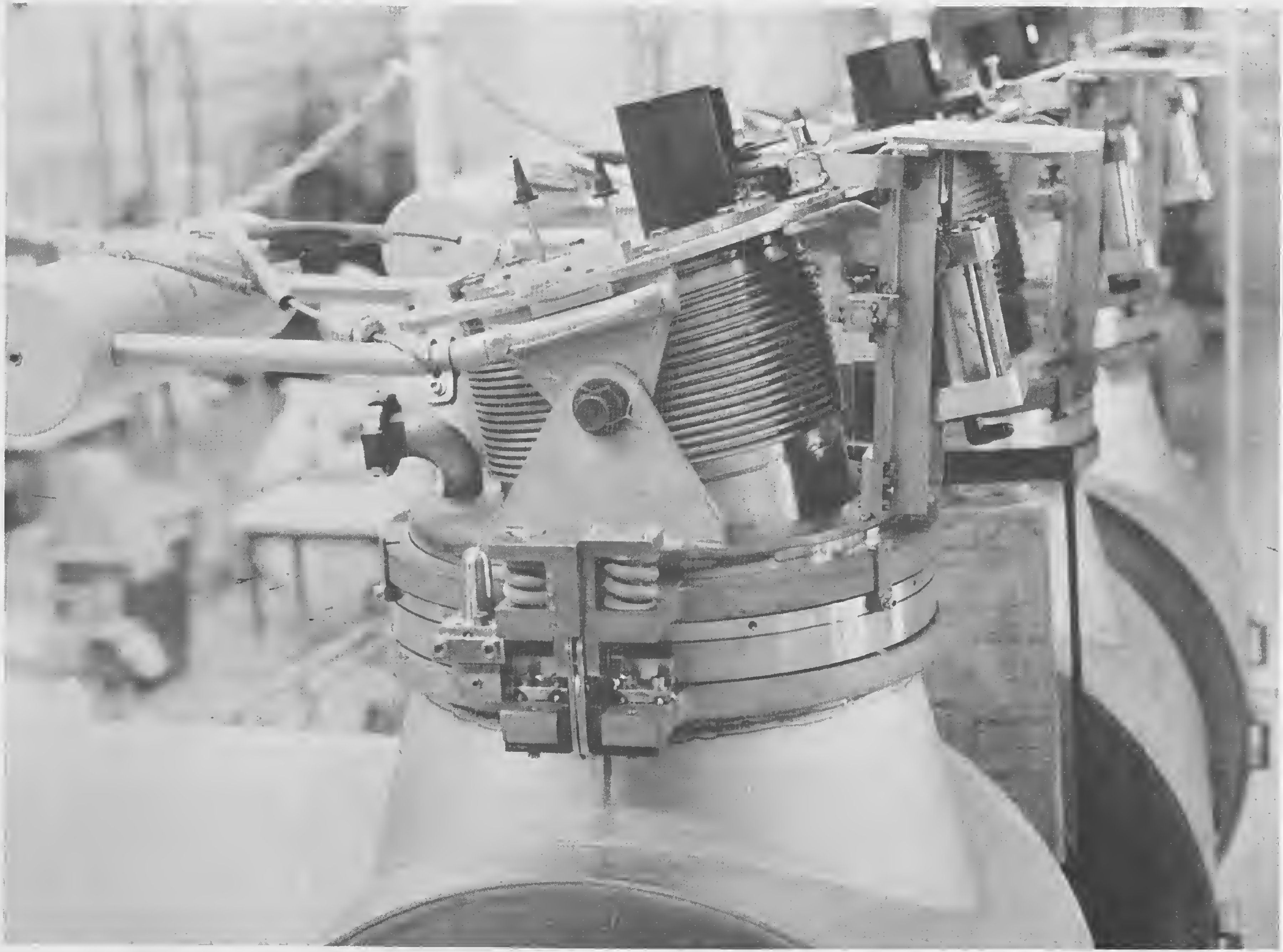
* This is what Leo chose to do. The two pads shown at 90° from the ears in Exhibit 10 bear on "ejection pins" in the flanges during relaxation of the ring at disassembly.



<u>Exhibit 1:</u> SLAC Designed Couplings Assembled to Cerenkov Cells. Non-Standard Guides and Guide Pins are Used with these Coupli s.

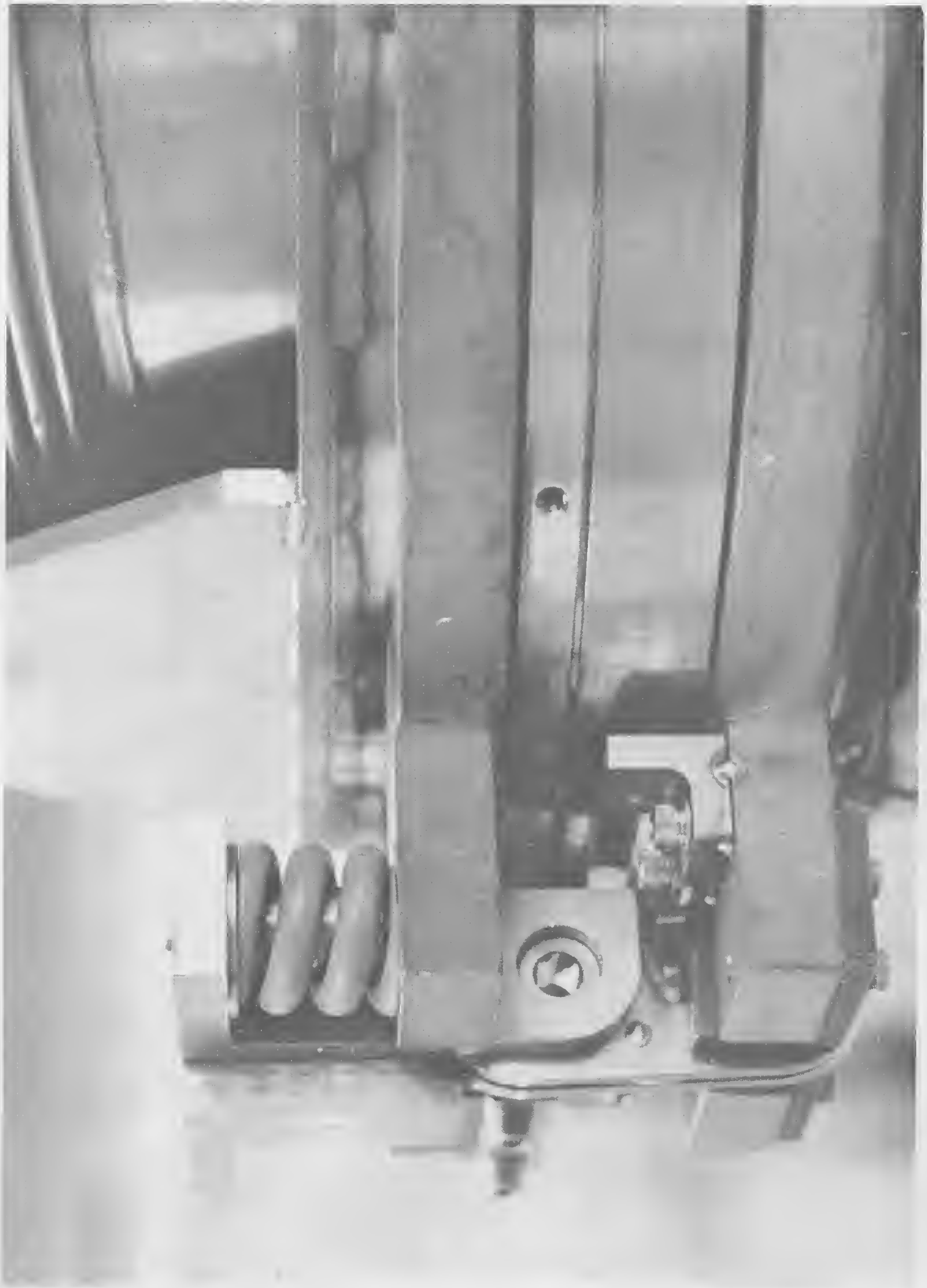


Exhibit 2: Cammed Hooks Compress Coil Springs which Load Ring-Springs. The Male Flange is the Thicker of the Two Flanges.

Exhibit 3: Female Flange, Ring-Spring and Standard Guide Pins. The Indium Has Not Been Inserted in the Groove. Four Clips (Female Flange Only) Hold the Ring-Spring to the Flange.

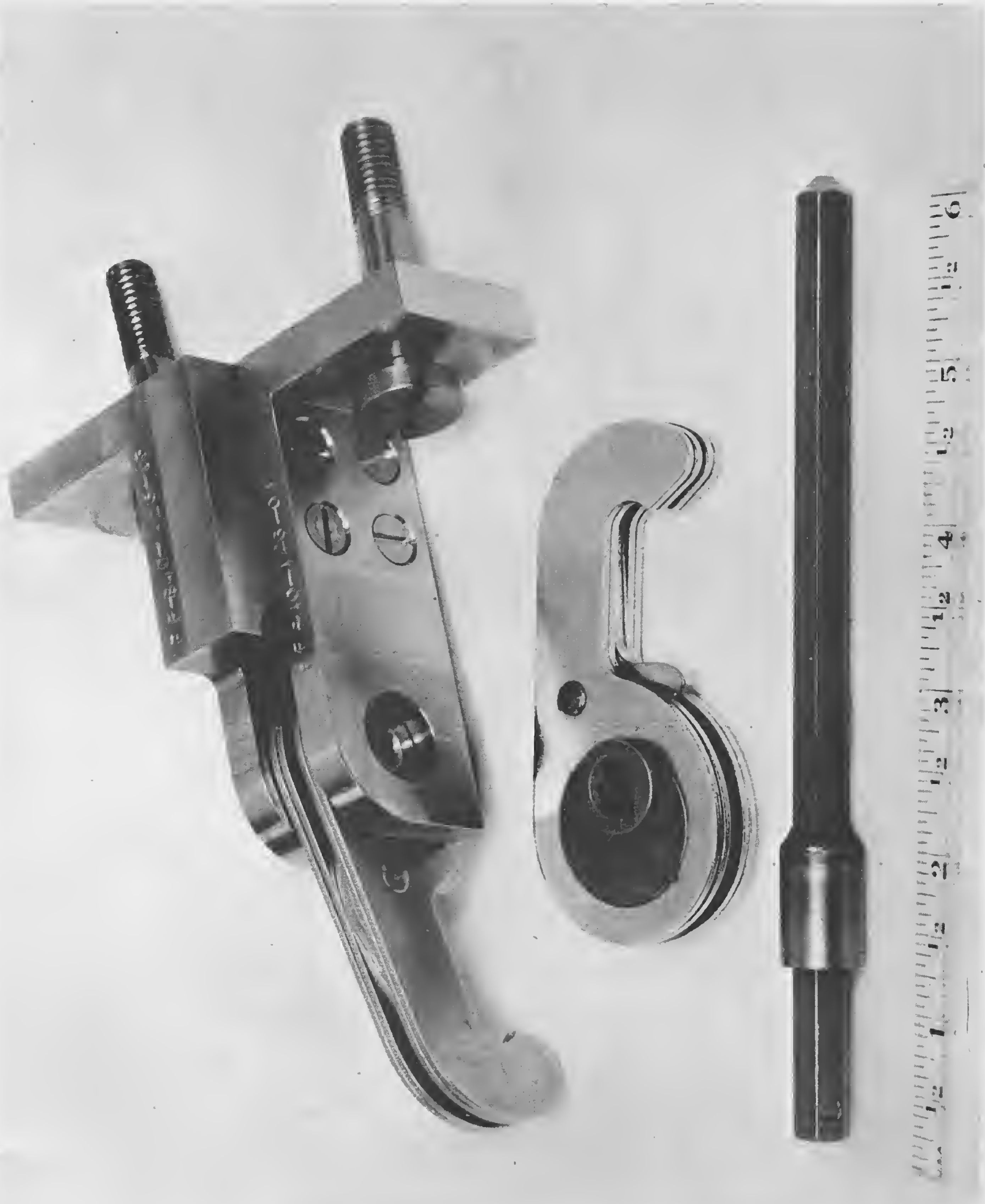


Exhibit 4: Cammed Hooks and Actuator.

TN-65-93
R. S. Miksch
December - 1965

Quick Disconnect Vacuum Couplings for the Beam Switchyard

1. Design Criteria

The beam switchyard of the accelerator contains many instruments and other components that may require maintenance or modification involving removal of the components from the beam tube. To facilitate removal and replacement, couplings were desired that could be remotely actuated and that would seal reliably with minimum rework of coupling features between closures. Criteria for coupling selection included:

1. Maximum leakage 10^{-4} std. cc. per second per joint, with a leak rate of 10^{-6} std. cc./sec./jt. as an objective.
2. Insensitivity to radiation (thereby excluding organic materials).
3. Suitability for remote operation.
4. Ruggedness.
5. Relative cost.
6. Size (particularly axial length).

2. Coupling Description

Consideration of the criteria led to the conclusion that only a gasketed coupling could be used under beam switchyard conditions, and that a metallic gasket must be used in the radiation environment. Tests of commercial couplings and of SLAC-designed prototypes indicated that indium gaskets were superior to others for this application involving poor accessibility to couplings. Suitable indium-gasketed couplings were not commercially available, so a coupling design was developed at SLAC to specifically meet the list of criteria. The basic design was applied to three pipe sizes -- 6", 10", and 12" diameters.

The SLAC-designed quick disconnect vacuum coupling for the beam switchyard consists essentially of a female flange having an indium gasket pressed into a groove on the mating face, and a male flange with a raised ridge, or knife edge, on its face to bear against the indium. The female flange is normally installed on the most readily removable system components. A pilot arrangement allows the flanges to be properly mated with relatively coarse manipulations. After mating, the flanges are clamped together with cammed hooks. Spring-loading arrangements on the hooks allow for slight differences in hook engagement dimensions and for knife-edge penetration into the indium.

The clamping loads must be applied around the entire circumference of the flanges to assure knife edge penetration into the indium. However, the couplings will be used in many locations that are accessible from only one side, and often, only with long-handled tools or manipulators. Therefore, the number of cammed hooks per coupling was limited to two. The hook force is distributed around the circumference either by flange stiffness, as in the 6" size, or by auxiliary "ring springs", in the 10" and

12" sizes. The ring springs are concentric with the flange hubs, and have bosses at 60° in both directions from the hook engagements. When the two hooks are cammed in their engaged positions, the bosses provide a six-point equal distribution of the hook loads.

3. Sealing Test Results

a. Six-inch Coupling

The six-inch coupling has been tested more extensively than the larger sizes, primarily because of convenience and schedule. In July of 1964, a six-inch coupling was operated through a series of 90 closing and opening cycles, during which no replacement or resoration of the indium was made. Periodically, the sealing effectiveness was checked with a leak checker. During the first 80 cycles, the highest measured helium leak rate was 5.4×10^{-6} std. cc./sec., but after 90 cycles, the leak rate was too great to be measured and the gasket was replaced. During this series, loads on the coupling were essentially those of the clamps and 600 lbs. due to air pressure on the end of the pipe.

In another sequence, five tests were made with the same coupling, with an 850 lb.-in. bending moment applied through the coupling. No helium leakage greater than 4×10^{-8} std. cc./sec. was reported.

The six-inch coupling was subjected to temperature excursions between freezing and 200° F. while evacuated, with no measurable leakage, even after application of the 850 lb.-in. bending moment.

Tests were also made with an aluminum knife edge in the six-inch size, to determine whether aluminum couplings could be used in certain instances. No change in behavior was apparent, and in one instance, the seal continued to function well while heated to 280° F.

b. Ten-inch Coupling

The ten-inch coupling was the least-tested size because it was felt that tests on both larger and smaller couplings would reveal any problems attributable to size. The coupling had a maximum helium leak rate of 1×10^{-5} std. cc./sec. during a series of five closures. On the sixth cycle, excessive leakage was measured. The flanges were rotated 90° with respect to each other, and the seal was again satisfactory, but this remedial step cannot be applied in practice, so the result is of limited value. A total of eight closures was made during a four-week period.

c. Twelve-inch Coupling

A series of ten closures was made on the twelve-inch coupling, with no intermediate restoration of the sealing surface. Maximum helium leakage rate 5×10^{-5} std. cc./sec. was observed on the ninth closure. In this series, atmospheric load on the pipe end was additive to the clamp load. Duration of the test series was five days.

A fixture was made with which the atmospheric load was isolated from the coupling by means of a bellows. Then, a series of seven closures was made, using only spring clamp load. During these tests, bending moments up to 600 lb. ft. were applied to the coupling, in various directions. At this maximum bending moment, helium leakage was measured as 1×10^{-5} std. cc./sec.

4. <u>Indium Gasket Behavior</u>

Atmospheric load on the pipe end was found to be sufficient to secure a good seal during the first coupling assembly after indium installation. After opening and reclosing the coupling, seal effectiveness was variable until the clamp force was applied.

Cold flow of the indium was evidenced by continued penetration of the knife edge for a few days. The penetration was rapid at first, and then diminished, as would be expected. The rate and extent of cold flow was quite dependent upon the number of previous closures to which the coupling had been subjected. The full penetration range of the knife edge appeared to have been used in only one instance during the above tests -- that was on the six-inch coupling, after 90 closures. In the bending moment tests with the 12" coupling, reported above, .004 cold-flow penetration was measured adjacent to the clamps in the first six hours, during which four closures were made. During the next forty days, in which three additional closures were made and bending moments up to 600 ft.-lbs. were applied in various directions, an additional .011 penetration was measured.

Indium adhesion to the knife-edge was not a problem during the test program, although couplings were seldom clamped for more than a few days without being opened. In one instance, the six-inch coupling was continuously clamped for about six weeks, and opening forces were greater than usual, but no adhesion was observed and the indium remained in its groove (which has 5° re-entrant sides).

A six-inch coupling assembled to a current intensity monitor remained sealed for approximately two months. Forceful uncoupling was required, but again the indium stayed in place, and only traces of adhesion to the knife edge were observed.

A twelve-inch coupling assembled to a Cerenkov cell exhibited extensive indium adhesion to the knife edge when opened after about two week's closure. It was determined that its closure had been made within minutes after a new indium surface was exposed by knife cut. Since newly cut indium appears to pressure-weld to itself much more readily than does old-cut indium, the time of air exposure may be a significant factor in indium adhesion.

Indium gaskets have several times been successfully reconditioned by folding the extruded material back into the knife-edge indentation and pressing as in the initial installation. Restorations have also been made by filling the indentation with indium wire or sheared slivers, and pressing as before. Pressing force used is of the order of 2,000-3,000 pounds per lineal inch of gasket.

Sealing reliability of restored gaskets has been found lower than for newly installed gaskets. The indium additions sometimes fail to bond to the parent surface. This may be caused by either air entrapment or surface contamination. The unbonded areas offer potential leak paths and also allow delamination to occur when the coupling is opened.

5. Indium Installation

Indium has been emplaced in the female flange by hand tamping, by arbor press, by roller, by melting, and by impacts from a pneumatic vibrator. The first three methods have given good results, while melting and vibration impacts have been only partially successful. Emplacement by rolling is currently considered the best production method, while hand tamping is used for special circumstances.

Hand tamping is effective, but slow and tedious. Arbor pressing is relatively fast and very effective, but is impractical for flanges attached to bulky devices. Rolling is intermediate in speed but good results have been obtained and many tooling variations are possible. Melting restricts flange orientation, is subject to shrinkage, may allow bubble entrapment, and may lead to surface contamination by scumming when done in air. Smooth surfaces have been obtained by vibratory impact, but light impacts do not eliminate sub-surface voids, and heavy impacts can be damaging to associated equipment.

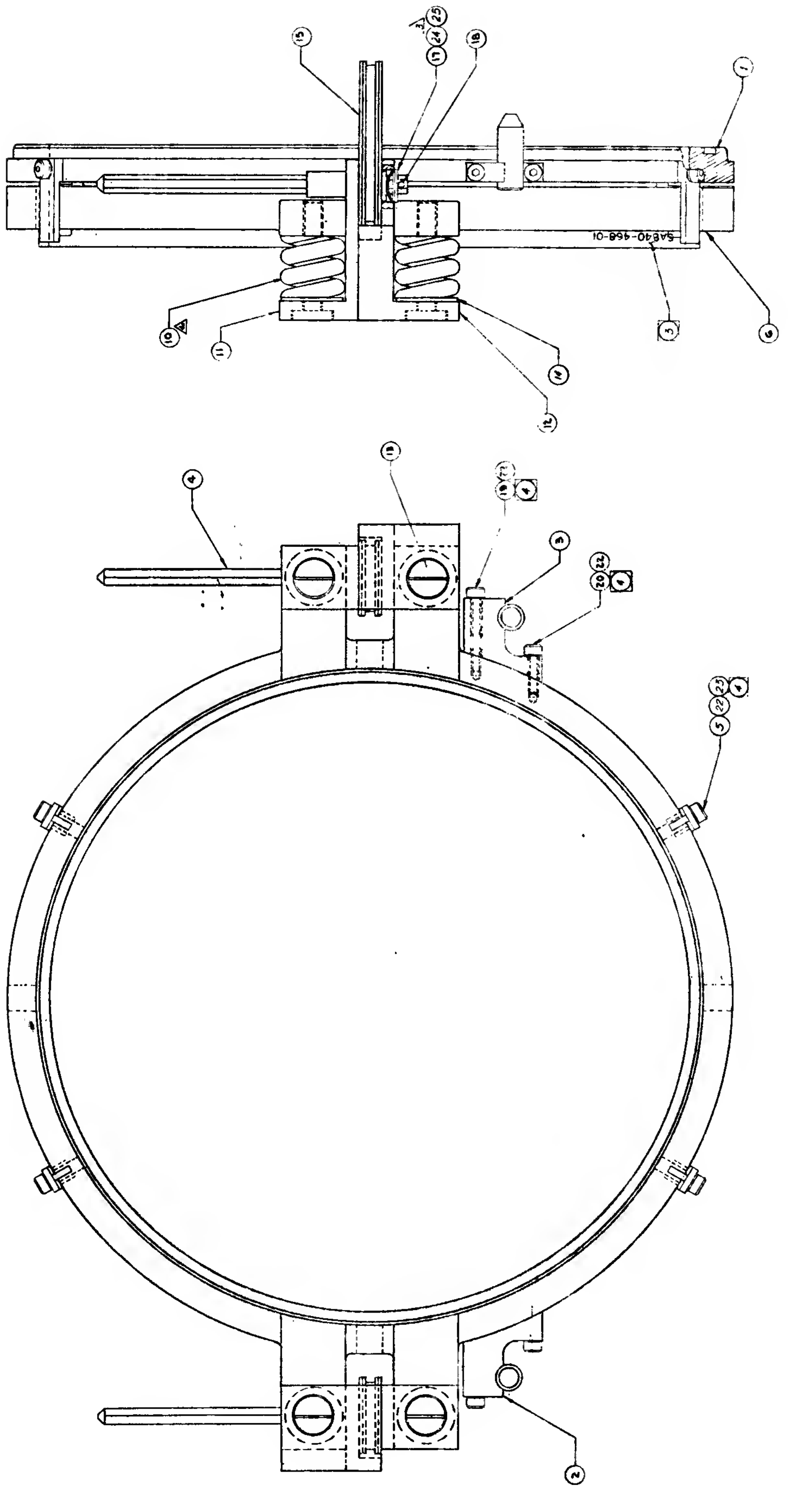


Exhibit 6: Assembly Drawing of Female Coupling Half.

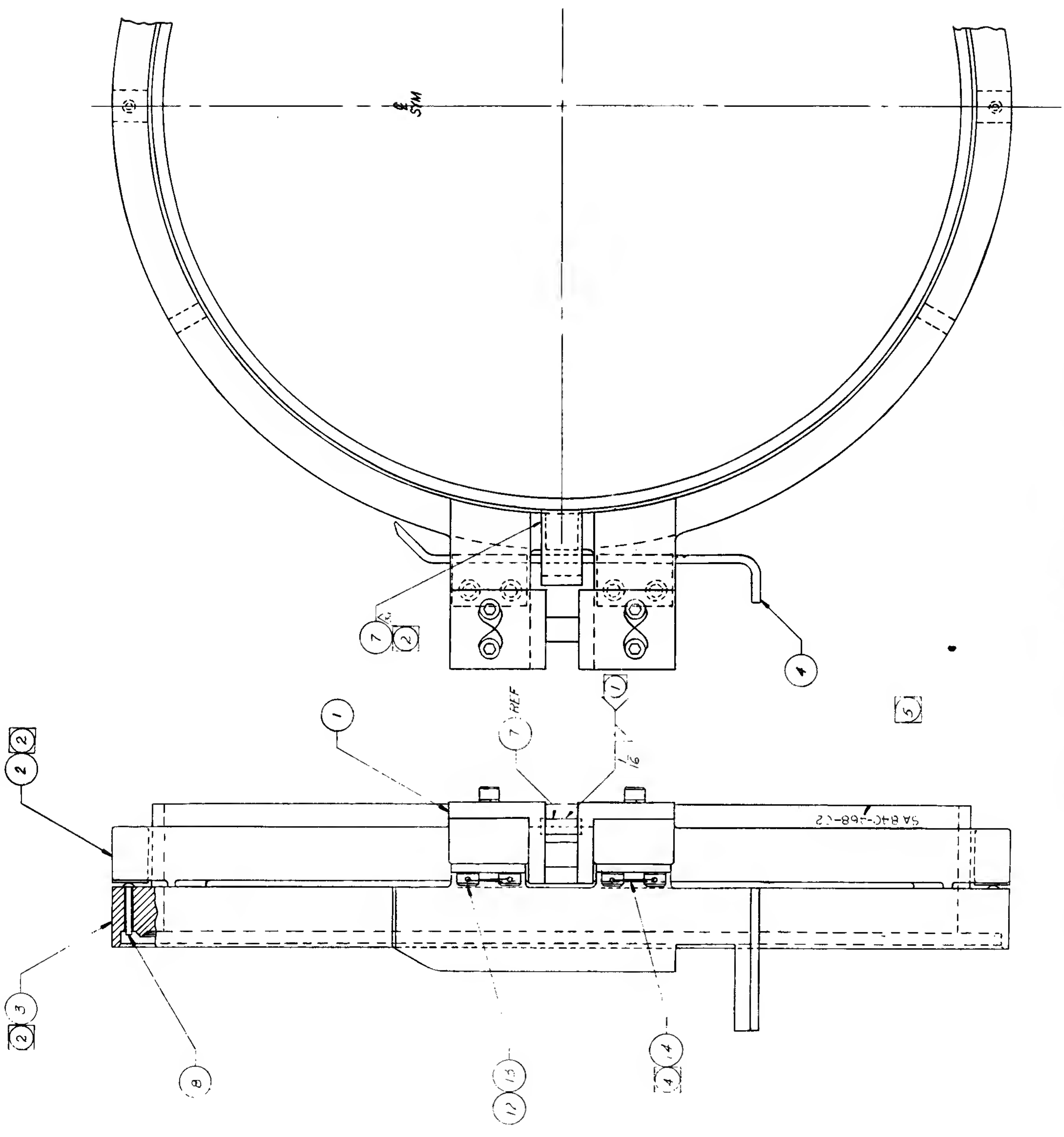


Exhibit 7: Part of the Male Half Assembly Drawing.

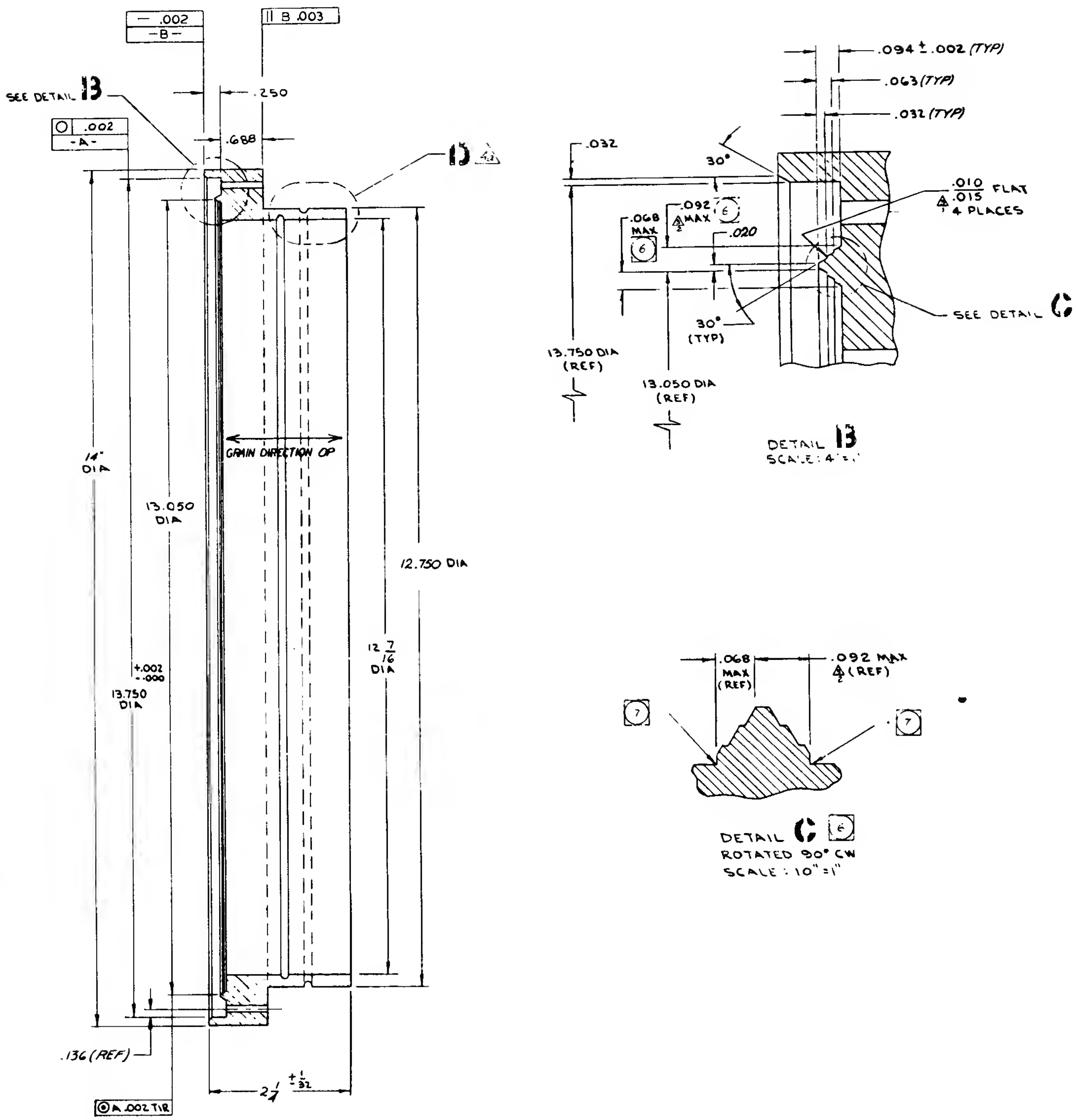


Exhibit 8: Male Flange Details.

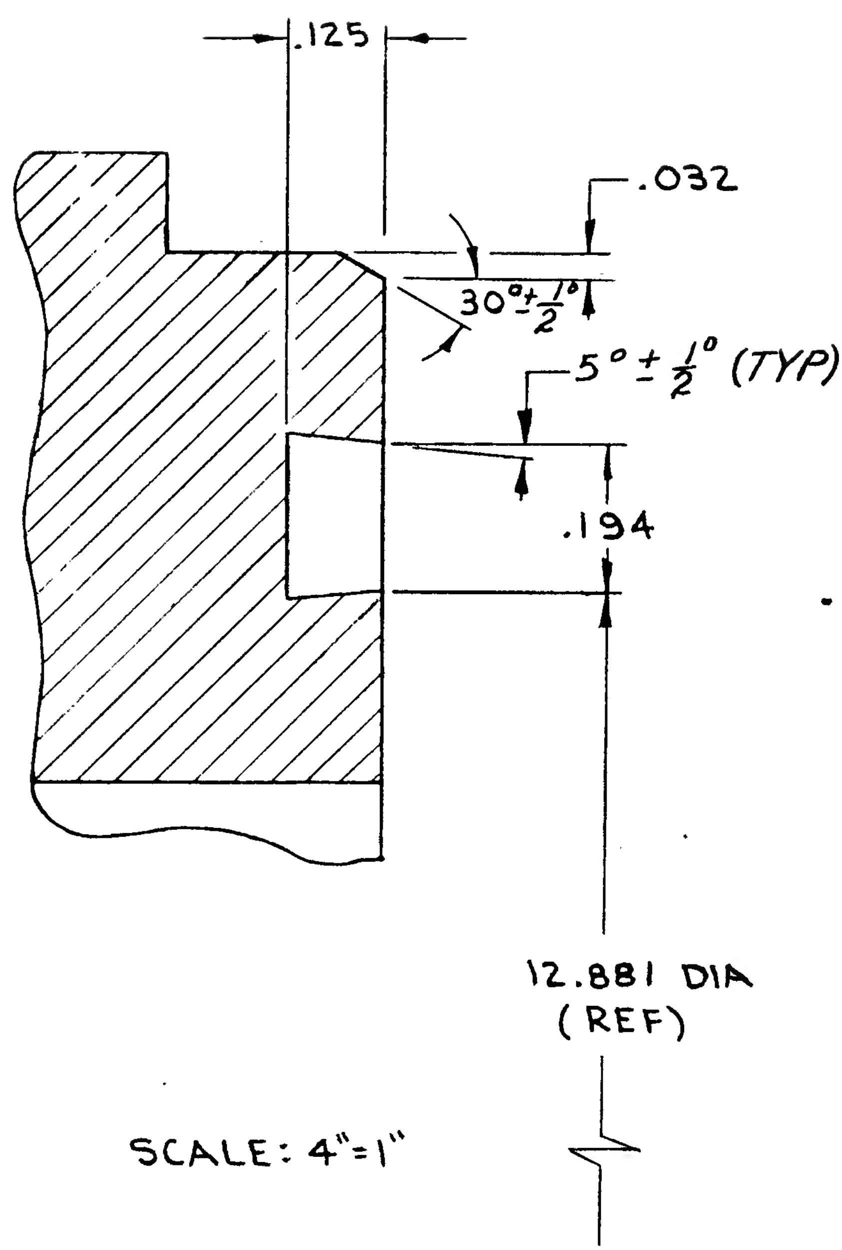


Exhibit 9: Detail of Groove for Indium in Female Flange.

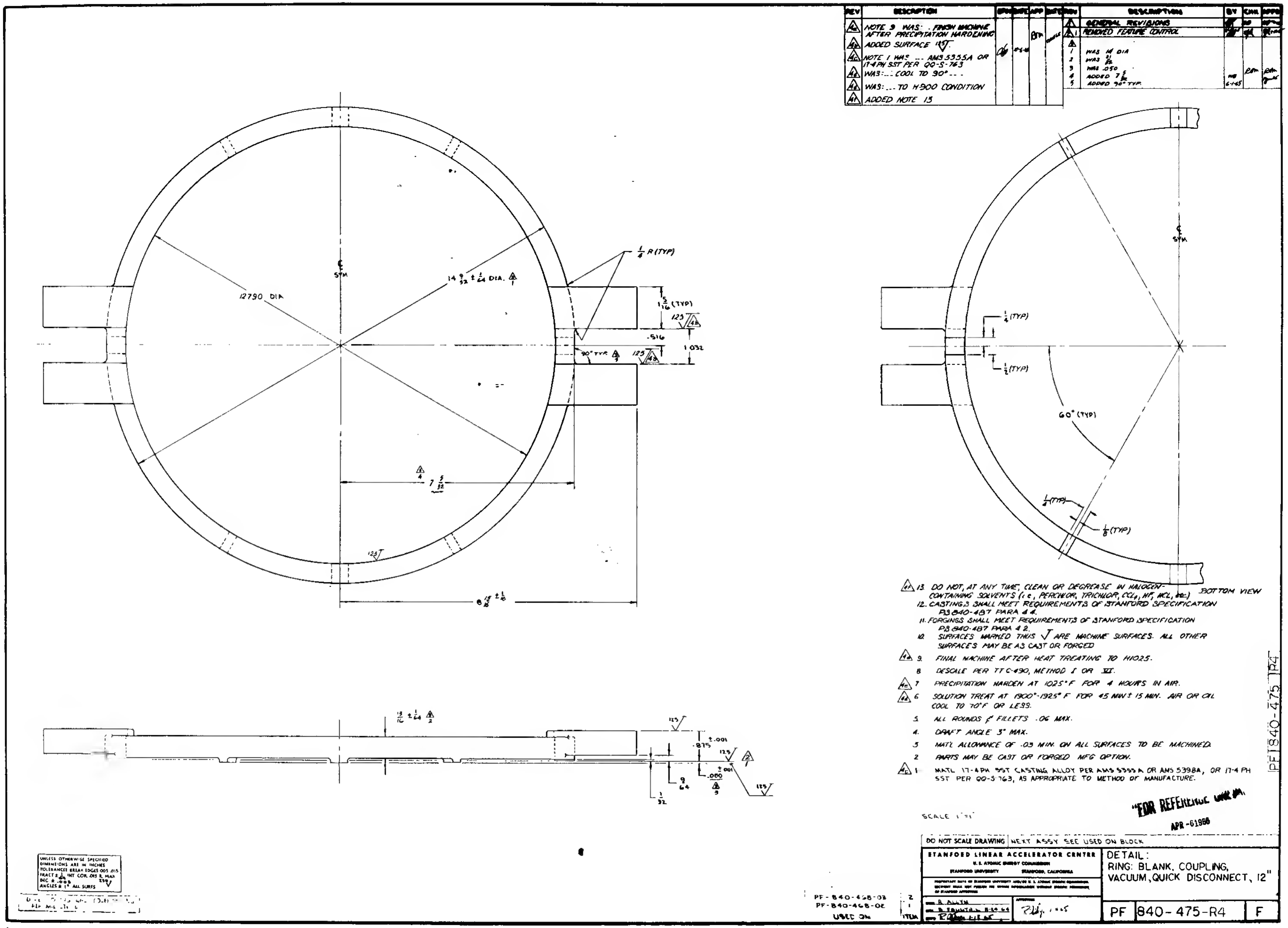


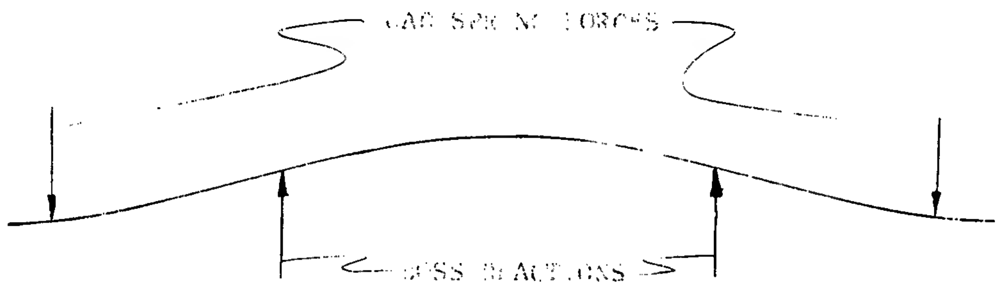
Exhibit 10: Ring-Spring.

STANFORD LINEAR ACCELERATOR CENTER (C)

Development of a Remotely Operable High-Vacuum Coupling

Ring-Spring Analysis and Load Spring Design Problems

Leo Bloom was unable to find a solution to the problem of a ring loaded with two forces on one side and four on the other in any of the reference books. To select a cross section, he therefore straightened the ring out and treated it as a simple uncurved beam loaded as shown below:



Leo said, "I talked this approximation over with two or three other SLAC engineers, and they all thought it would be a workable approach."

Knowing the seal circumference and desiring a force of 100 lb per lineal inch of seal, he made calculations and designed a ring with an axial height of 21/32 (.656) inch and a radial thickness of .635 inch for the 12 inch coupling. The first prototype ring-springs were machined out of stainless steel plate to this size. The two bosses beneath the ears were .050 inch below the plane of the other four bosses.

By the time the prototype rings had been made, Russ Miksch had taken over the project. Russ wanted to experimentally determine how much force would have to be applied at the two ears to get six points of equal loading on a flange. He loaded a ring-spring in a Baldwin testing machine until the bosses beneath each ear were just co-planar with the other four bosses. Then he applied 50% more load, assuming that this would result in equal loads at the six bosses. This equal load situation was the desired one for an installed coupling, but Russ found that the ring-spring was not nearly stiff enough, as it gave a force of only 20 to 25 lbs per inch of seal under these conditions. This discovery was made in May 1965, several weeks after a contract for the manufacture of the couplings had been awarded.

Here Russ said, "The range of bids we got for the couplings was unusually wide -- there was almost a 3 to 1 difference between the highest and the lowest. Perhaps some of the shops overestimated the problems they might run into. Also, the lowest bidder was substantially below the second lowest bid. But the low bidder had quite a bit of experience in machining stainless steel to close tolerances and it seemed to us that they were fully capable of doing the job, so we accepted their bid."

The total cost of a 10 inch coupling complete with both flanges is about $475 in the quantities involved; the 12 inch coupling costs $550. A 6 inch coupling, with no ring-springs, costs $250. SLAC ordered 190 10 inch couplings and 197 of the 12 inch variety.

Russ asked the vendor to delay subcontracting the ring-springs to a foundry for casting while he redesigned them. He found that the ring-spring dimensions were limited both radially and axially by clearance requirements, so he went as far as he could in each direction. This resulted in a new cross-section for the 12 inch ring-spring with an axial height of 13/16 inch (.813, up from .656) and a radial thickness of .775 (up from .635). He also planned to decrease the heights of the bosses beneath the two ears by .030 inch, making them a total of .080 inch lower than the other four, to further increase the load transmitted to the flanges.

Russ wanted to get an idea of how much stiffer this ring-spring would be before making a prototype. He found a handbook solution for the deflection of a ring loaded by opposed forces applied in pairs at four equally spaced points. He computed a solution to this problem for the first ring-spring and then compared the calculated deflections with his test results for the actual six-point loading. He said, "I found the factor by which the spring rate for the four-point loading case could be transformed to that for the six-point case. Then I computed the four-point spring rate for several new rings and multiplied by this factor to get an approximation to the actual six-point loading condition. What I did was extrapolate from a known solution to an unknown one."

Several months later Russ made an outline of this computation to document the procedure should it be desired to use it again; this outline sheet appears in Exhibit 1.

Russ's calculations showed that a new ring with a 13/16 by .775 cross section would give a force of about 80 lbs per inch of seal when the boss heights differed by .080 inch and a load 50% beyond that required to bring all six bosses coplanar was applied. He then had a ring-spring of these dimensions made; upon testing it he found that the seal force was about 70 lbs per inch. Russ said, "There were ways we could have gotten more force from the ring, but we reviewed our seal test data and decided that 70 lbs per inch would probably give a satisfactory seal. We didn't have the time or money for another redesign. There's also some uncertainty in both the calculations and the spring rate tests. Elasticity moduli are not known very well -- uncertainties are perhaps one part in 30 -- and the modulus changes with heat treat, among other things."

Although Russ never tried to compute the stress in a ring-spring, he loaded one in the testing machine to ten times the design load without encountering any yielding.

It had been originally planned to load the ring-springs with stacks of Belleville springs instead of the coil springs shown in the exhibits of Part B. Leo Bloom had wanted to use Bellevilles so he could tailor a force-deflection curve that would be almost flat. He said, "I wanted only a small change in force over a considerable range of deflection. As the knife edge penetrates the indium, the deflection decreases while the area of the knife edge in bearing increases. You could say, as a first approximation, that penetration will stop when the force exerted by the springs equals the yield force for the indium -- that is, its yield stress times the projected area of the imbedded knife edge. What we want is a static situation. Unfortunately, indium creeps at room temperature, although nobody seems to know just how much. Since deflection, and force, decreases with penetration while area increases, we tend towards the kind of static equilibrium conditions we want."

After Russ Miksch had completed the ring-spring tests he was ready to design the Bellevilles. The force required of them had not been known until this time. Russ also knew the space available in terms of a maximum allowable O.D., a minimum I.D. and a maximum length. First he calculated a single Belleville that would give a force-deflection curve of the shape he wanted. Then, for the 12 inch coupling, he stacked six of these in parallel (force additive, deflection unchanged) to give the required force. A single group of six would not provide enough deflection so he used four of these groups in series, where their deflections would be additive but the force would be the same as for a single group. This arrangement is shown in the photograph of a prototype coupling which appears in Exhibit 2.

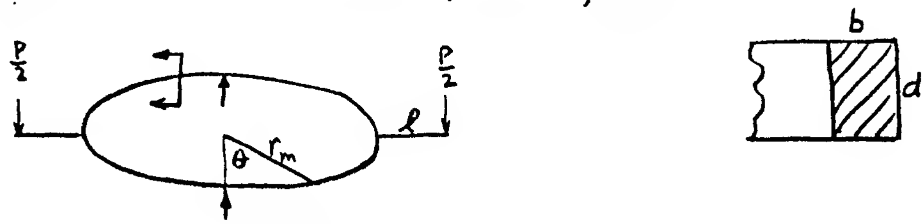
Russ Miksch said, "We were going to order 38,000 Bellevilles, but we found that the big spring manufacturers usually make all their Bellevilles with linear deflection curves and our order wasn't big enough for them to change their ways. But there were a couple of more important reasons why we had to go to coil springs. With the stacked Bellevilles we got a big hysteresis loop in the force-deflection curves because of inter-spring friction. We really didn't know <u>what</u> force we were getting on the indium. The hysteresis could have put us off by as much as 50% and we never knew where on the loop we were. The other problem was the stress in the springs. We had planned to make them from 17-7 PH stainless, but when we got around to making calculations we found failure would be almost certain; it looked like the theoretical stress at the outer edge of the Bellevilles would be around 270,000 psi and 17-7PH is unusually susceptible to stress-corrosion cracking."

It was October 1965 when Russ found that the Bellevilles would not work out as planned. He said, "We had built several prototypes using Bellevilles, but I'm not sure now what kinds of forces we were actually developing. By the time we decided to go to coil springs, we were constrained to the same package size, since a lot of the coupling parts had already been built. The spring had to fit around a 5/8 inch bushing; the

overall O.D. we could use was limited to a little more than 1 1/4 inches and the allowable length was about the same. I also knew that if we had to go to a chrome silicon valve spring steel to get enough strength, I'd have to find some way of protecting the springs against corrosion."

STANFORD LINEAR ACCELERATOR CENTER DESIGN DATA	DATE 16 SEP 65	D.D. NO.	PAGE 1/1
SUBJECT: RING SPRING RATES & LOADS	PREPARED: R. MIKSCH		
	CHECKED BY		

REF. PRODUCT ENGINEERING, JAN. 7, 1963



① DETERMINE PROPERTIES OF SECTION

MEAN RADIUS $r_m = \frac{OD + ID}{4} = \frac{ID + b}{2}$

MOMENT OF INERTIA $I = \frac{bd^3}{12}$ WHERE b = RADIAL WIDTH, a = AXIAL THICKNESS

FACTOR $K = ab^3\left\{.333 - .21\frac{b}{a}\left[1 - \left(\frac{b}{1.86a}\right)^4\right]\right\}$

WHERE a & b ARE RESPEC. LONG & SHORT SIDES OF RECT. X-SECTION

FACTOR $\lambda = \frac{EI}{GK}$ WHERE E & G ARE MODULI IN TENSION & TORSION (SHEAR)

LEVER ARM ℓ = DISTANCE FROM SECTION ℄ TO LOADING POINT.

② DETERMINE SPRING RATE AT $\theta = \frac{\pi}{2}$ POINTS FOR LOADING AT 90° INTERVALS, IN OPP. DIR. AS SHOWN

$$\frac{P}{y} = \frac{16\,EI}{r_m^2\left[1.1416\,\ell(1+\lambda) + r_m(2.283 + .566\lambda)\right]}$$

③ MULTIPLY ② BY 2.03 TO FIND SPRING RATE FOR LOADING AT 60° INTERVALS AS IN SLAC SPRING.

④ DETERMINE LOAD TO BOSS-CONTACT BY MULTIPLYING AVAILABLE DEFLECTION BY SPRING RATE ③

⑤ DETERMINE TOTAL REQ'D APPLIED LOAD FOR 6 PT EQUAL LOADING BY MULTIPLYING ④ BY 1.5

⑥ DETERMINE SEALING FORCE PER INCH OF SEAL CIRCUMFERENCE BY DIVIDING ⑤ BY SEAL CIRCUMF.

⑦ IF ⑥ IS UNACCEPTABLE, CHOOSE NEW X-SECTION AND TRY AGAIN.

Exhibit 1: Outline of Ring-Spring Extrapolation Calculation.

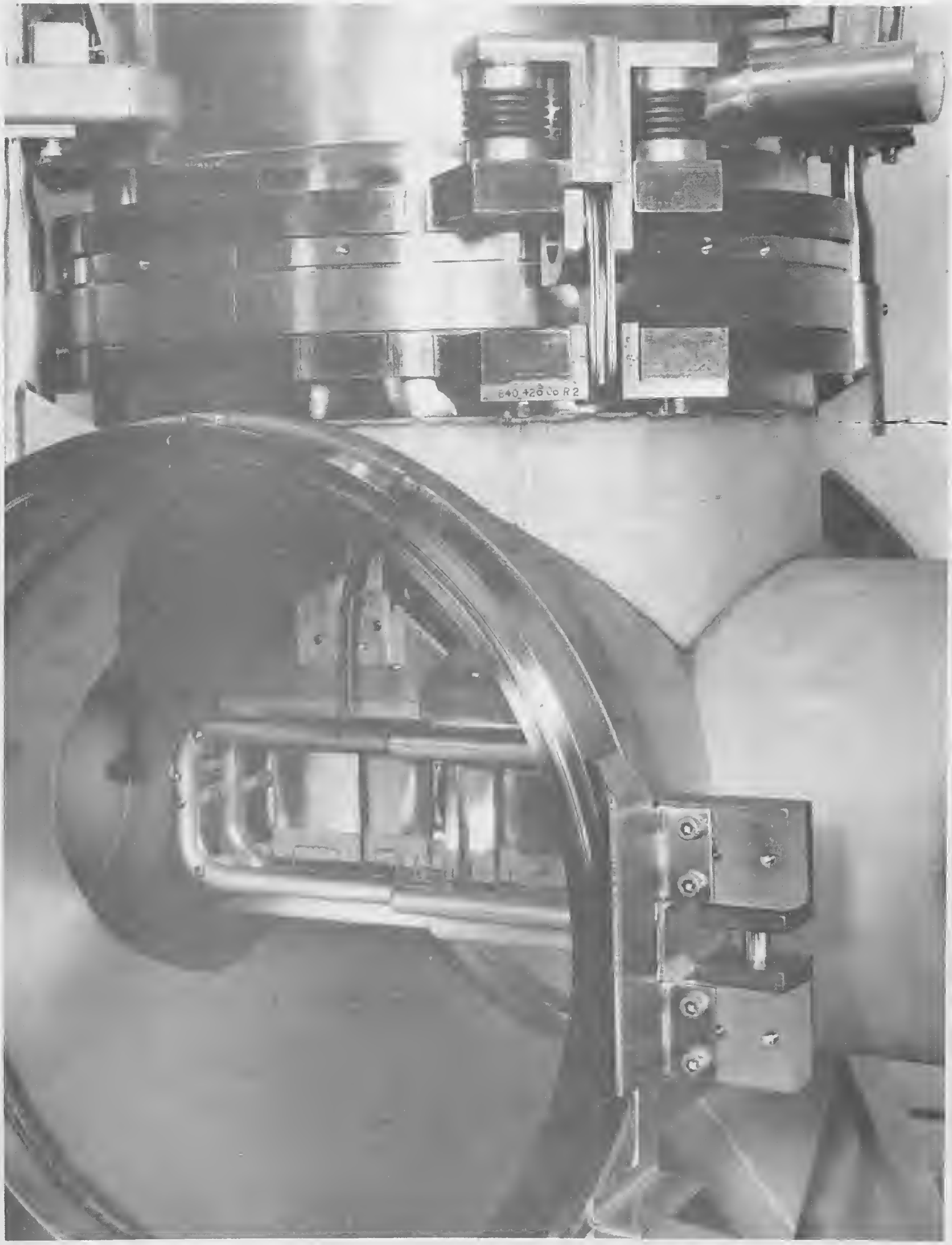


Exhibit 2: Coupling with Belleville Load Springs. Lower Part of Photograph Shows a Male Flange with a Standard Sheet Metal Guide.

STANFORD LINEAR ACCELERATOR CENTER (D)

Development of a Remotely Operable High-Vacuum Coupling

Coil Spring Design

The coil spring Russ designed is shown in Exhibit 1. There are about 3.3 active coils and because of the space limitations no closed coils are used at the ends. The material is a 9254 chrome silicon steel. Russ said, "After winding, the springs are subjected to an operation called, variously, scragging, set removal, cold setting or pre-setting. They're loaded until the coils are closed up and held for a short period of time before release. This causes some local yielding in the outer surfaces of the coils and gives them a permanent set. Afterwards, in use, the coils can be closed up with little danger of further permanent deformation."

He continued, "When the cam goes over center, the torsional stress reaches a maximum of 130,000 psi, then it relaxes a little. Of course, with such a low index, it was necessary to apply the Wahl correction factor. I investigated stainless steels for the springs, but they wouldn't take the stress, so I had to go to the 9254. As it is, we're using about the stiffest spring that could be put into the space available."

Russ first thought of chrome plating the springs to protect them from the corrosive environment of the beam switchyard (the radiation ionizes the air to nitrous oxide which can then combine with moisture to form nitric acid). But he said, "I was told that it's very hard to plate nickel and chrome on springs and get a uniform covering without bare spots, so this didn't look like a good approach. Gold plate gives excellent protection against corrosion, but it's awfully expensive. Then someone recommended Sperex paint to me; this is intended for high temperature use, but it had been used at SLAC for corrosion protection on some of the structures with good success. I decided to run a comparative test on a bare spring, one that had been gold plated and two painted with Sperex."

Russ's tests were made in February 1966. He supported four partially compressed springs over a vat of 37-1/2% nitric acid. The springs are shown after the test in Exhibit 2. The gold plated spring on the left had been exposed for 13 days. Second from left is a spring which had been exposed for 8 days after being wet blasted and then receiving two coats of Sperex paint. The next spring also got two coats of Sperex, but was not wet blasted first. It was exposed for 10 days. On the right is a plain, uncoated spring after an exposure of 8 days. The same four springs are shown in Exhibit 3 after the surface scale had been removed. Russ said, "The gold plate did a good job, but the plating costs $3 a spring

for a half-mil layer and the spring itself only costs $1.35. Under a microscope, the Sperex paint didn't seem to cover very well and I thought it showed up rather poorly in the tests. After being left over the acid for only 12 hours the paint changed color. Then several people suggested that I try the radiation resistant vinyl paint used on the walls in the high radiation areas of the accelerator. This turned out to work very well and is what we're using on most of the springs. We give them two coats, for a thickness of about 5 mils. On a few very critical couplings we're first gold plating the springs and then giving them a coat of the vinyl paint simply for mechanical protection."

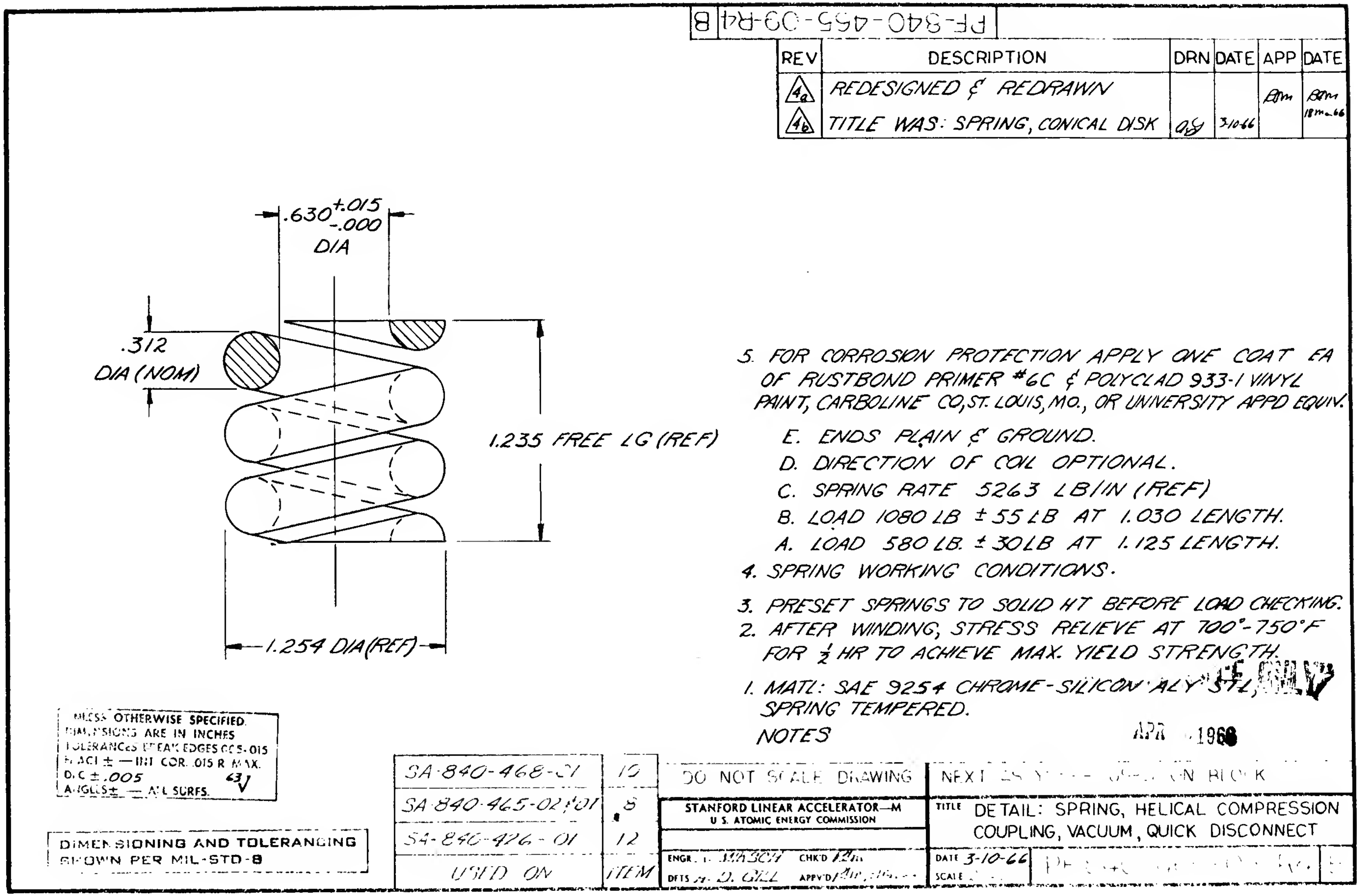


Exhibit 1: Coil Spring.

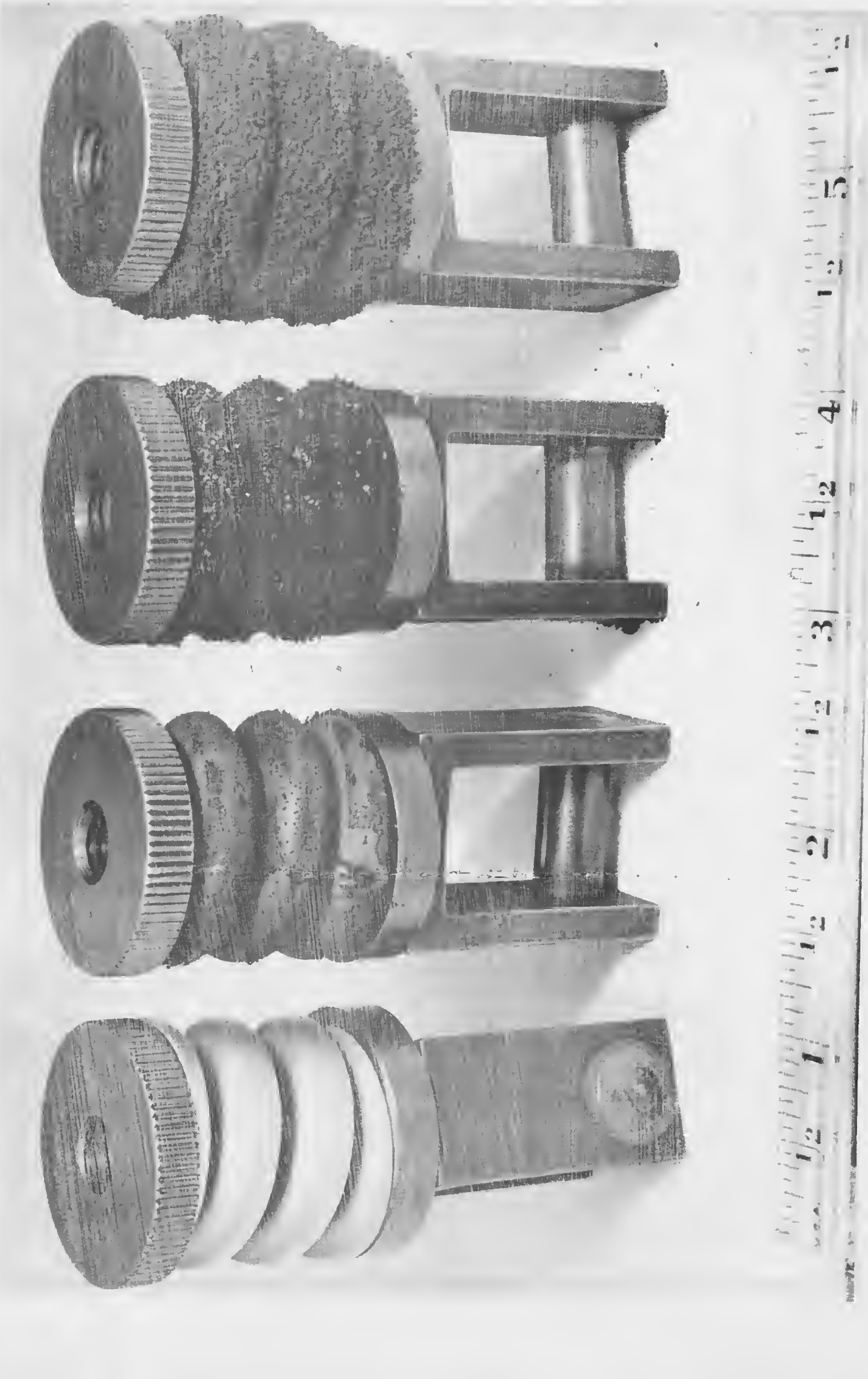


Exhibit 2: Springs After Corrosion Test.

Exhibit 3: Corrosion Test Springs After Scale Removal.

STANFORD LINEAR ACCELERATOR CENTER (E)

Development of a Remotely Operable High-Vacuum Coupling

Material Selection

A high-vacuum coupling designed at the Stanford Linear Accelerator Center (SLAC) for use on steering magnets and instruments in the beam switchyard at the end of the accelerator is shown in Exhibit 1. It is remotely operable from manholes up to 10 feet away. A keystone recess in one of the mating flanges is filled with indium; a stepped knife edge on the other flange is imbedded in the indium to form the seal. Male and female flanges are shown in Exhibit 2. Flanges for couplings with I.D.'s of 10 inches or 12 inches are clamped together by cammed hooks which load coil springs. The coil spring force is distributed to the flanges by annular ring-springs because the engineers felt that the flanges alone would not be stiff enough to give an even force distribution around their circumferences. A 12 inch ring-spring is shown in Exhibit 3. Six raised bosses on each ring-spring contact the flange. The resulting force is about 70 lbs per lineal inch of seal. Although the stress in the rings was unknown, Russ Miksch, the Project Engineer, believed it to be relatively low, most likely below 15,000 to 20,000 psi.

G.P. Fritzke, a SLAC metallurgist, was faced with a certain lack of knowledge regarding the long term effects of the radiation environment when selecting a material for the ring-spring, as well as for the flanges and other miscellaneous parts. The flanges are welded to the equipment or pipe. He knew that although the radiation level in the switchyard would vary greatly with location, it was expected to be typically around 10^{11} rads for a 10 year period, where one rad equals an energy absorption of 100 ergs per gram of material. Mr. Fritzke also knew that the radiation would act on the air to form nitrous oxide, which could then combine with moisture to form nitric acid, a potential corrosive agent. He further anticipated the possibility of other corrosive acids, formed similarly. In selecting a material for the ring-springs, Mr. Fritzke planned to consider not only corrosion resistance, but also strength, manufacturing methods, stress corrosion, hydrogen embrittlement and cost.

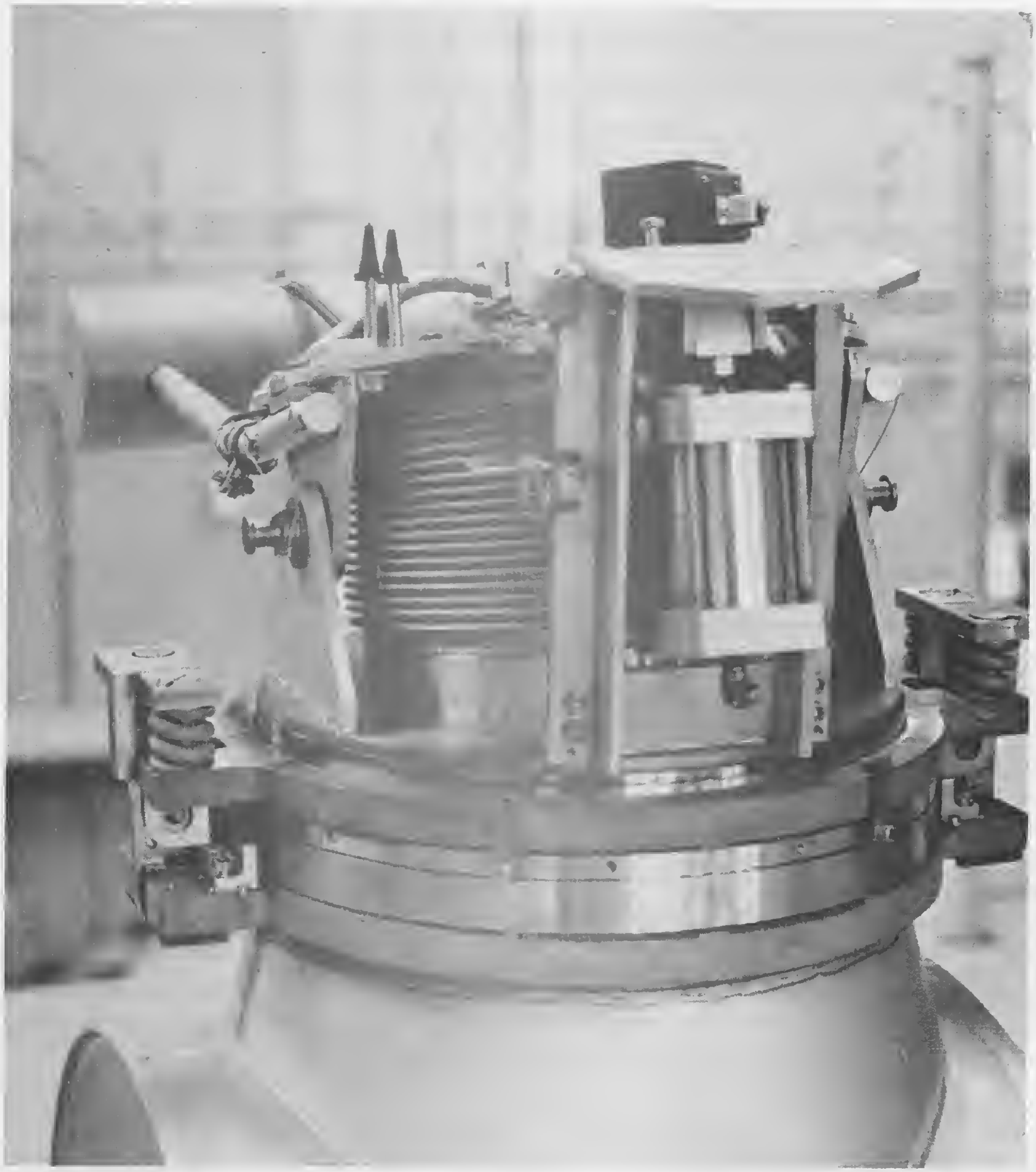


Exhibit 1: Coupling Assembled to a Cerenkov Cell.

Male Flange with Ring-Spring and Sheet Metal Guides to Aid Assembly Installed.

Female Flange with Ring-Spring and Guide Pins Installed. The Groove has not been Filled with Indium.

Exhibit 2: Male and Female Flanges.

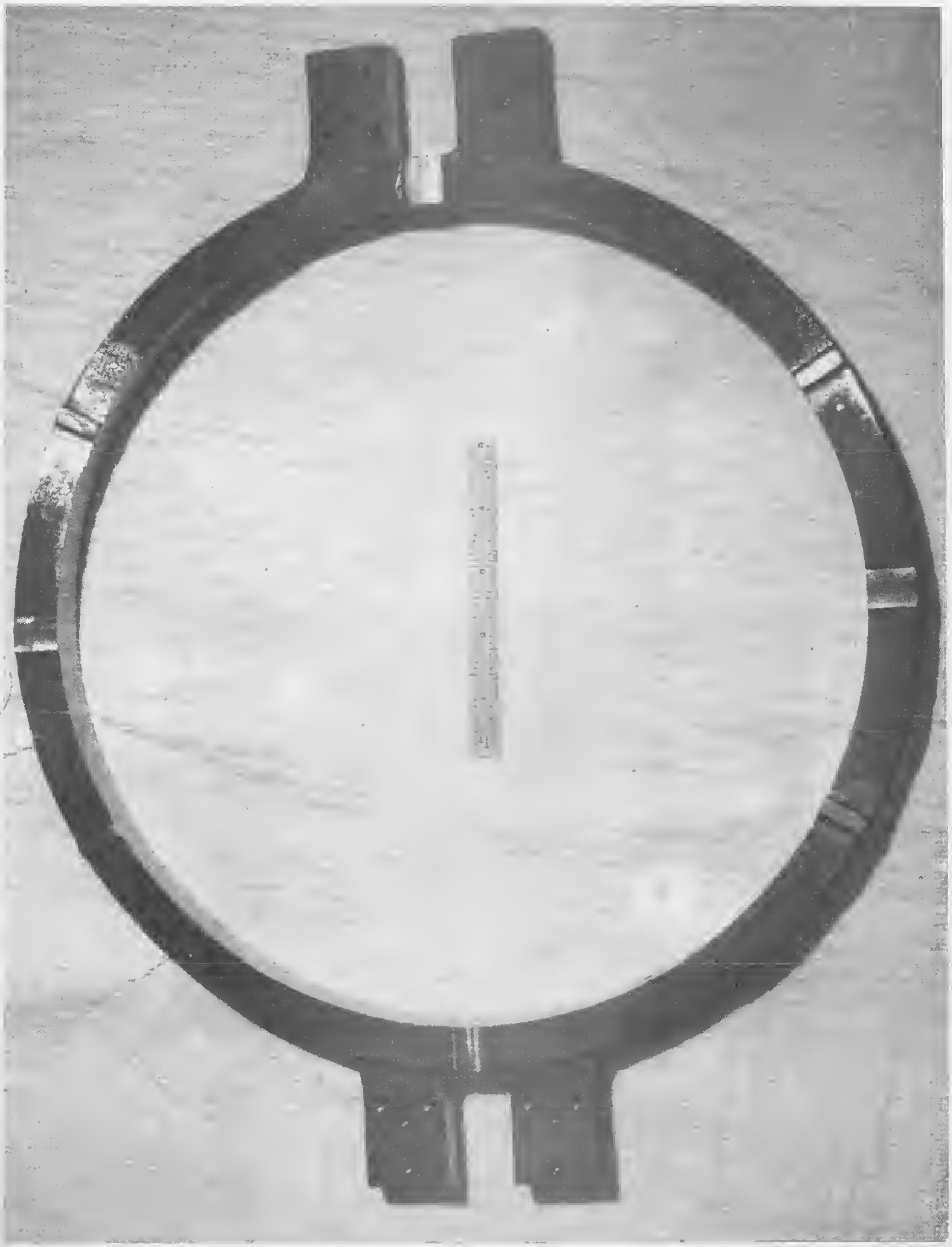


Exhibit 3: Twelve Inch Ring-Spring.

STANFORD LINEAR ACCELERATOR CENTER (F)

Development of a Remotely Operable High-Vacuum Coupling

Ring-Spring Material Selection and Casting Problems

The materials selection study carried out during the spring of 1965 by SLAC metallurgist G.P. Fritzke, justified Leo Bloom's (the original project engineer) previous choice of 17-4PH stainless steel for the ring-springs. Leo had chosen this steel on the basis of high strength, corrosion resistance, ease of casting and reasonable cost. Mr. Fritzke's much more intensive investigation bore out Leo's judgement. There had been no metallurgists available to aid Leo during his original design work; Mr. Fritzke had been consulted by Russ Miksch when Russ became worried about the effects of stress corrosion on the 17-7PH stainless chosen for the Belleville load springs he was then planning to use. In November 1965 Mr. Fritzke prepared the technical note which appears in Exhibit 1 to document the ring-spring material selection. The coupling flanges are 304L stainless because this offers better welding characteristics. The cammed hooks and other miscellaneous parts are all either plated or made of stainless steel.

In June 1965 Russ Miksch and a representative of the firm which had contracted to make the couplings for SLAC visited several area foundries and selected one they thought would do a good job of casting the ring-springs. The vendor foundry used a shell mold process in which patterns identical to half the ring are bolted to a backing plate and then sprayed with a mixture of sand and resin. They cast the rings eight at a time using four gates for each ring, one on either side of each set of ears. The castings produced were unsatisfactory, with cracking around the ears as shown in Exhibit 2 and centerline shrinkage resulting in voids which showed up in X-ray photographs. Although Russ gave the foundry permission to increase the radii of the fillets around the ears, the problems persisted through the summer and no satisfactory ring-springs were delivered. At least a few were needed for use on prototype couplings and trial installations, so in August about a dozen were made in SLAC's shops by rolling a bar to a circular shape, butt welding its ends together and welding on ears. Russ said, "For a while I considered making all the ring-springs this way, but we soon found that the fabricated rings would cost 6 to 8 times as much as cast rings. Also, I knew that the metallurgists warned against welding the rings because of the possibility of stress corrosion cracks originating in the non-homogeneous weld areas. The ring section was also too deep for complete penetration of the weld."

In September Russ and the vendor for the coupling decided there was no hope of getting good ring-spring castings from the foundry they had originally chosen, which seemed to be beset by management problems. A foundry in San Diego had been recommended to the coupling vendor, and Russ made an inspection trip to this foundry with a representative of the vendor. The job was given to the new foundry, which proposed to cast the rings individually at the rate of 40 per week. They used a CO_2 mold process in which fine sand mixed with a binder is pressed around wooden patterns. A reaction with CO_2 gas sets up the binder. Ten gates were used around each ring. This foundry delivered satisfactory castings with virtually no evidence of cracking or shrinkage. However, they only managed to produce about 20 rings per week. Russ said, "It seems you run into this kind of thing all the time. They seemed to be doing the best they could, but they had a new furnace ordered to help them meet production, and delivery on this was a couple of months late. Then they found its power supply cooling system inadequate, so there was another delay for modifications."

By April 1966 many of the couplings had been installed and Russ expected no more problems of any great consequence, although he said, "We'll have to keep an eye on all the materials because of possible degradation in the radiation environment. It's of course impossible to make long term tests beforehand and with some things, for instance the vinyl paint on the coil springs, we really don't know what will happen."

TN-65-88
G.P. Fritzke
November 1965

Considerations in the Selection of Ring-Spring Materials for the Quick Disconnect Vacuum Coupling

SUMMARY

During the evaluation of candidate materials for the ring-spring portion of the quick-disconnect vacuum coupling, several problems associated with the BSY environment were investigated. The materials were evaluated with regard to their response to: 1) general corrosion, 2) stress corrosion, and 3) hydrogen embrittlement. 17-4 PH stainless steel was selected as the ring-spring material. Also covered are discussions with the casting vendor and a short heat-treatment study to investigate modifying the 17-4 PH cast structure. Recommendations for a surveillance program are also made.

I. INTRODUCTION

A quick-disconnect vacuum coupling has been designed to facilitate the rapid insertion and removal of monitoring equipment and experimental apparatus in the Beam Switchyard (BSY). A critical portion of this assembly is the ring spring which distributes a uniform peripheral load to the assembly from compressed coiled springs. These coiled springs transmit their force to the ring springs through a connection near the ring-spring "ears". In service, the load from the compressed springs will be distributed from the ring ears to the assembly by means of eight (8) raised lugs positioned around the ring spring.

The choice for the ring-spring material, 17-4 PH stainless steel heat treated to an H900 condition, was reviewed in light of expected environmental conditions in the Beam Switchyard. Also considered were other possible material choices for the ring springs, alternate heat treatments, and the effects of manufacturing processes upon the material selected.

Included, therefore, in this report is a short discussion of the major problems which may cause premature ring-spring failure and their relationship to the selected material. The BSY environment, which is now only generally understood, limits the possible ring material choices available with a less stringent environment, so a wide range of potential problems possibly relating to other BSY components was included in the study. Briefly, the problems which were reviewed included general corrosion mechanisms, stress-corrosion cracking, and hydrogen embrittlement. At the end of the report, suggestions are given which may help to avoid catastrophic down time due to failures from an unexpected cause.

II. DISCUSSION

In order to properly evaluate choices for the ring-spring material, it was first necessary to define the BSY environment and the possible effects of this environment on various materials. Following a short discussion of these effects, the merits of several candidate materials follow. Finally, a summary of discussions with the ring-spring casting vendor and the results of a short heat-treating study on the selected material is presented.

A. Beam Switchyard Environment

In addition to normal room temperature air containing a normal amount of moisture, the general BSY environment will include several chemicals produced by particle bombardment from the high-energy electron beam. It has been noted[1] that the first, and probably most prevalent, of these chemicals to be produced will be nitrous oxide which, in the presence of water, will form nitric acid. Other possible chemicals that may form will be oxides of sulfur and tellurium from dry lubricants and possibly HCl from residue films left on components after various cleaning and degreasing

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[1] G.C. Rogers, "Memo on Nitric Acid Corrosion in BSY", Sept. 21, 1965.

processes. All of these chemicals produce corrosive acids when combined with water.

Also to be considered as part of the BSY environment is the electron beam and its generated stream of particles. According to Neet[2] a fast neutron flux up to 10^{19} neutrons (2×10^{11} rads) can be expected. The effects on materials of construction will be to embrittle and strengthen, but after some period of time, element transmutation will begin to alter alloy structure. The net effect of these changes will be to generally degrade the material properties from their original design criteria.

B. Possible Environmental Effects

Of the many possible environmental effects that can change the role of constructural materials, three effects important to the BSY application will be considered here. These are general corrosion, stress corrosion, and hydrogen embrittlement.

1. General Corrosion Problems Related to the Stainless Steel

It was assumed that the most prevalent corrosion medium in the BSY would be nitric acid. Stainless steels were first used commercially on a large scale in service involving nitric acid, and continue to be used in such installations. These first applications were of 15% to 18% Cr steel (now type 430) and soon thereafter of 182 Cr - 8% Ni steel (now type 304). The necessity for proper heat treatment to prevent accelerated corrosion and intergranular attack of these steels in nitric acid was demonstrated at once through service failures of improperly heat treated and as-welded equipment. These difficulties were eliminated by post-fabrication heat treatments involving slow cooling from about 1450°F for type 430, and rapid cooling from about 2000°F for type 304 stainless steels. Subsequently, for the austenitic grades, the use of stabilizing elements (particularly columbium in type 347) and, more recently, reduction of carbon content to 0.03% max (type 304L) have been effective in controlling this problem without the necessity for quenching fabricated equipment from a high-temperature heat treatment. In the as-welded condition, 304L and 347 show satisfactory resistance to corrosion by nitric acid and are therefore suitable for field-erected equipment.

Type 304 in the annealed and water-quenched condition has essentially the same resistance to corrosion by nitric acid as types 304L and 347, but type 304 should be heat treated after fabrication to prevent intergranular corrosion.

The stainless steels are relatively insensitive to factors such as the aeration, velocity and agitation of an aqueous fluid, since nitric acid is oxidizing and tends to favor passivity. Neither pitting nor stress-corrosion cracking is a problem under these circumstances. However, nitric acid causes intergranular attack in unstabilized stainless steels that contain more than 0.03% C, unless they

2 D.A.G. Neet, "Radiation Exposure in the Switchyard", SLAC-TN-65-9, Jan. 1965.

have been properly heat treated. The presence of hydrofluoric acid in nitric acid, as in certain pickling solutions, increases such attack. Hydrofluoric acid also increases the rate of general corrosion, as do appreciable amounts of other halides.

In hot dilute mixtures of nitric and sulfuric acids, no appreciable attack occurs on the stainless steels when the ratio of nitric acid to sulfuric acid is about 2 to 1, or higher. This is one of a number of examples where sufficient nitric acid will prevent attack that would otherwise occur. With very dilute hot mixtures of sulfuric acid and nitric acid (about 1 to 1.5% total acid), where the proportion of nitric acid will not maintain passivity for the austenitic grades, type 443 (20% Cr, 1% Cu) has greater corrosion resistance.

Therefore, from a general corrosion standpoint, nitric acid prooves to be more helpful than harmful when most stainless steels are considered. However, considering the real effects in the BSY some time after operation start, the accumulation of dirt (from dust and corrosion products) and moisture and the degradation of the alloy character by the beam may change the corrosion resistance of the alloy. Pitting corrosion, caused by the formation of areas where dirt causes the chemical stability of the oxide film on the stainless steel to be destroyed, may be encountered. Also, the alternate wetting and drying of components when the atmospheric humidity changes will serve to form localized concentrations of various acids. Accelerated corrosion can result from large differences in such electrolyte concentrations. Another corrosion mechanism that may be encountered includes pitting corrosion from alloy inhomogeneities in metal due to inclusions, coring, and distorted zones.

A by-product problem of using stainless steels is the cathodic (protected) nature of most stainless steels toward other anodic (corroded) materials. (Indium, used as the vacuum seal in the assembly, may be quite anodic to stainless steel.)

In addition to those corrosion problems mentioned above, dissolved oxygen in an acid solution can act as a cathodic depolarizer as it continuously removes hydrogen from a corroded zone. The removed hydrogen, which would otherwise retard galvanic corrosion by neutralizing the anodic/cathodic potential, will be replaced by newly-generated hydrogen by a continuous corrosion mechanism.

Stress-corrosion problems were thought to be sufficiently important to be considered separately.

Although the corrosion mechanisms mentioned above are but a few of the possible mechanisms which may be encountered as singular, or most probably as multiple, problems, the exact mechanisms which will be encountered in the BSY are impossible to predict. The very fact that most corrosion problems, specifically those related to material failures, are diagnosed after a material fails, illustrates the difficulty in predicting the exact problem and the relative ease of diagnosing failures. These seemingly-obscure corrosion mechanisms are included not to create any undue anxiety but to illustrate the many forms of corrosion and the difficulty in pinpointing the problem in a "simple" system.

2. Stress-Corrosion Cracking

Stress-corrosion cracking has been defined as the complex interplay of tensile stress and corrosion which leads to cracking in a metal or alloy. In the absence of a corrosive environment, the material exhibits normal load-carrying properties.

Tensile stresses must be present at the surface of the metal for stress-corrosion cracking to occur. Such stresses can be classified as either applied or residual. External loading inducing an applied stress in a member is easily visualized. Residual stresses, on the other hand, are developed in the metal by some operation in the metal's prior history which has caused heterogeneous yielding on the part of the metal. These residual stresses are divided into macrostresses and microstresses.

Macrostresses result when various components of an assembly restrain each other and may result also from nonuniform deformation incurred in fabrication operations. They may be developed also through heterogeneous deformation induced by thermal gradients in heat-treating operations.

Microstresses involve microscopic regions of a metal, such as grains and parts of grains. They also arise during forming operations, because individual grains do not deform with perfect homogeneity. The process of plastic deformation involves internal disturbances, such as slip, twinning, warping of crystal planes, orientation effects, and fragmentation of grains. Microstresses of atomic dimensions are associated with dislocations.

Of particular interest, from a metallurgical standpoint, are those reactions involving volume changes. In the case of the precipitation hardening steels, where a second phase is precipitated from a solid solution during cooling, each particle of precipitate will be compressed by the matrix if the second phase occupies a greater volume than the components from which it is formed. The matrix thus will be subjected to tensile stresses. Complicating factors are: 1) difference in coefficient of thermal expansion of different phases; 2) plastic flow in a hot and ductile interior; 3) suppression of phase transformation by a cooler rigid shell.

The quenching of steel from the austenitic state to form martensite also involves volume expansion. This results in the formation of the tetragonal lattice of martensite and the generation of an internal stress.

Other variables which often interact in stress-corrosion cracking are:

1) Alloy composition
2) Corrosive environment
3) Temperature
4) Time

Stress-corrosion cracking is not limited to an aqueous environment if attack by liquid metals, molten salts, and organics is considered

in the broad classification of corrosion. Thus, cracking failures of brass in mercury, steel in molten zinc, stainless steel and titanium in molten chlorides, or titanium rivets in molten cadmium (from cadmium coatings) can be considered examples of stress-corrosion cracking.

Cracking in aqueous solutions is the most common form of stress-corrosion encountered. In addition to aqueous media, certain moist gases can promote this type of attack. However, most alloys are known for their cracking susceptibility in certain environments.

It should be noted that stress-corrosion cracking does not always occur in the solutions most commonly listed for each alloy. In fact, as mentioned previously, it occurs only under certain critical combinations of factors (alloy composition, stress level, temperature, time, and solution composition and concentration). The concentration of the solution has been shown to have a marked influence on the stress-corrosion cracking susceptibility of some alloys. Table I lists some of the environments in which martensitic stainless steels have failed by stress-corrosion cracking.

TABLE I

Environments in which Stress-Corrosion Cracking of Martensitic (Chromium) Steels has been Observed, Other Conditions Being Favorable

Nitrates
NaCl
Chlorides
Fluorides
Bromides
Iodides
Seacoast Atmosphere
Industrial Atmosphere
Water and Steam
H_2S

Dissolved oxygen in the solution can have an effect on stress-corrosion cracking. A well known example is the increased susceptibility to stress-corrosion cracking of austenitic stainless steel in chloride solutions containing dissolved oxygen.

Cracking which occurs as a result of solution concentration can frequently be eliminated by design changes. Crevices which are possible sites for solution concentration should be eliminated wherever possible.

It is extremely difficult to predict when stress-corrosion cracking may develop in terms of projected service life of equipment. In fact, cracking failures in replicate specimens exposed under carefully controlled laboratory conditions often follow a statistical distribution curve, i.e., a few fail after a relatively short exposure and a few survive long-time exposure, while most specimens fail at some intermediate exposure time.

17-4 PH sheet material shows good resistance to stress-corrosion cracking in a marine atmosphere. Welding of the alloy in the highest strength condition (H 900) reduces the resistance to cracking. The standard solution heat treatment, following welding, apparently does not completely restore the resistance to stress-corrosion cracking of material. Therefore, in stress-corrosion environments it would appear to be safer to use material aged at higher temperatures, if the lower strength achieved under these conditions is acceptable.

In some instances, surface tensile stresses can be reduced by techniques such as shotpeening and tumbling which introduce compressed stresses in the surface. Corrosion, however, may remove this layer preferentially.

3. Hydrogen Embrittlement

Hydrogen embrittlement can become an important concern in the martensitic grades of stainless steel, generally increasing with hardness and carbon content. It becomes variable and less acute in ferritic steels, and is virtually unknown in the austenitic grades.

The embrittling hydrogen may be acquired as a result of the melting process, a heat treating atmosphere, or chemical and electro-chemical processes such as pickling and electroplating.

Most heat-treating atmospheres contain hydrogen in the form of a) moisture, b) hydrocarbons, or c) elemental H_2 as an atmosphere or a dissociation product. The use of hydrogen or cracked ammonia for bright annealing in one plant was associated with cracking of wire coils of types 431 and 440C, although other plants have reported no similar difficulty. Nevertheless, it is possible that some loss in ductility may result from the bright annealing of <u>any</u> of the martensitic stainless steels.

Steel which is subjected to a tensile stress exceeding some critical value and which contains hydrogen that is free to move is susceptible to failure in a delayed, brittle manner. The problem is especially serious because the minimum stress for failure decreases as the strength of the steel is increased, and because failures occur with no appreciable ductility, even though in a tensile test the material may exhibit normal ductility. Under most conditions the strength level of the steel is the most important factor affecting the occurrence of delayed, brittle failure. Both the minimum applied stress that will result in failure and the time required for the failure to occur decrease as the tensile strength of the steel is increased. These failures occur in all types of steel microstructures except austenite. Alloy composition is a relatively unimportant factor in the hydrogen-induced, delayed brittle failure of body-centered cubic steels.

It has been shown that such failures depend directly on the hydrogen content of the steel, and the way in which the hydrogen gets into the steel is of no importance. Failures do not occur if hydrogen is kept out of the steel or is removed before the steel is damaged permanently.

Normally, the critical amount of hydrogen required to induce failure is not present at the sites where failure initiates; hydrogen must move to these sites, either as the result of a hydrogen-concentration gradient or a stress gradient. The former condition prevails when the steel is exposed to an environment which permits hydrogen to enter its surface. Stress gradients that will cause hydrogen to move to regions of high tensile stress may result from bending or notches.

The problems in hydrogen analysis are very great as a result of the small amount usually present and the great mobility of hydrogen even at room temperature. Many investigations have relied upon such criteria as variations in cathodic charging time, variations in current density, variations in the concentration of, or time of exposure to, nonelectrolytic liquid environments (usually acids) as the basis for evaluating the effects of variations in hydrogen content. However, this is not easy to do because of the numerous processing operations that are potential sources of hydrogen and because of the very small amount of hydrogen (as little as 1 ppm, or possibly less) that can induce failure.

Table II lists some of the possible sources of hydrogen which may cause embrittlement in steels.

TABLE II

Sources of Hydrogen in Steel-Processing Environments

Steel-making operation - while steel is still molten
Fully killed steels where oxygen is removed which would normally combine with hydrogen
Liquid from rammed crucible refractories
Pickling operations
Cathodically-cleaned steel
Electrolytic machining
Electroplating
Moisture from water in coated welding electrodes
Heat treating at high temperatures in presence of low-pressure H_2
Cyclotron-proton irradiation

A representative 17-4 PH casting section was submitted for hydrogen analysis by the vacuum fusion technique. Results are not yet available.

C. Ring-Spring Material Selection

During the consideration of potential candidate ring-spring materials, several properties were considered. These were:

1) Room Temperature Yield Strength. As high a yield strength as possible, compatible with other considerations, is desired.
2) Room Temperature Toughness. A moderate degree of toughness is desired to withstand shock loads during assembly, etc.

3) Cost and Availability/Fabricability. A ring is required at reasonable cost commensurate with good properties. Casting is most desirable since other production techniques are either more expensive (closed-die forging, ring rolling) or compromise physical properties (welding). Machinability was also considered.
4) Corrosion Resistance. The resistance of candidate materials to the BSY environment and related problems were considered.
5) Weldability. Considered in the event that repairs or slight design modifications were required.
6) Radiation Damage Susceptibility. A difficult attribute to assign to material choices since limited quantitative information is available.
7) Other Effects. Susceptibility of choices to other normal processing environments, such as cleaning and degreasing solvents, electroplating problems (if required), etc.

A summary of the same considerations for the various alloy systems follows:

1) Titanium Alloys. Although these alloys exhibit good general corrosion and stress-corrosion resistance to the BSY environment and good room temperature strength (up to 175,000 psi yield strength), their high cost and fabricability problems (hard to properly weld) render them unsuitable.
2) Cobalt Alloys. These alloys are all too weak (80,000 psi yield strength) at room temperature. Besides high cost, the ultimate generation of copious amounts of Co^{60} also makes these alloys undesirable. Fabricability is also a problem.
3) Ferritic Stainless Steels. These steels are available and fabricable, but are too weak at room temperature to be suitable.
4) Austenitic Stainless Steels. The only technique to strengthen this class of steels above their moderate room temperature strengths is by cold working. 304 SS can be cold rolled to yield strengths up to 200,000 psi, but subsequent fabrication of plate (flame cutting, sawing, machining) raises costs appreciably. A possible exception is to ring roll by piercing and hot expanding a blank to size and then explosively forming to induce cold work. This relatively new technique is quite inexpensive but development time is not available. Austenitic castings are too weak for consideration.
5) Semi-Austenitic Precipitation Hardening Stainless Steels. PH 15-7 Mo and 17-7 PH steels are characteristic of this group and can be heat treated to yield strengths greater than 200,000 psi. However, the high susceptibility of these alloys to stress-corrosion cracking renders them undesirable.
6) Nickel-Base Alloys. Although somewhat more expensive than iron-base alloys, nickel-base alloys exhibit excellent strength, fabricability, and corrosion resistance in most environments. With yield strengths up to 170,000 psi, a nickel-base alloy would be an excellent choice. Lack of developmental lead time and relatively high cost render them a second choice to martensitic stainless steels.
7) Martensitic Stainless Steels. These steels are available as either quench-hardened or precipitation-hardened alloys. The quench-hardened

alloys, like grades 420 or 440 can be hardened to yield strengths up to 195,000 psi (with some sacrifice in ductility or toughness), but their corrosion resistance and fabricability are impaired. The precipitation hardening alloys, of which 17-4 PH is a typical example, can be strengthened to 180,000 psi yield strength with a fair degree of toughness. Available in castings whose properties approach that of wrought material, these alloys are quite fabricable and exhibit good corrosion resistance. Although martensitic grades are susceptible to cracking by hydrogen embrittlement, only the austenitic stainless steels are not susceptible and these grades are not readily hardenable. Table III summarizes the effect of fast neutron irradiation on 17-4 PH. Changes in all mechanical properties can be noted, but are not extreme.

TABLE III

Effect of Fast Neutron Irradiation upon the Mechanical Properties of 17-4 PH Steel[3]

Property	Exposure	Unirradiated	Irradiated	Change	% Change
Yield strength, psi	2×10^{19}	144.7	179.4	+34.7	+23.9
Tensile strength, psi	"	148.5	181.0	+32.5	+21.9
% Elongation	"	16.0	13.0	-3.0	-18.7
% Reduction of Area	"	65.0	53.0	-12.0	-18.5
Hardness, R_c	"	26.0	32.0	+6.0	---
Yield strength, psi	1.3×10^{20}	144.7	194.5	+49.8	+34.5
Tensile strength, psi	"	148.5	197.0	+48.5	+32.7
% Elongation	"	16.0	12.0	-4.0	-25.0
% Reduction of area	"	65.0	44.0	-21.0	-32.3
Hardness, R_c	"	26.0	33.0	+7.0	---

8) Other, Nonstandard Grades of Wrought Steels. Other grades of stainless steels, such as AM-350 and AM-355 are available with yield strengths and toughnesses similar to 17-4 PH. These alloys are also castable and weldable. Rene -41, Udimet 700, D-979 and Unitemp 212 are a series of Fe-Ni-Cr-Mo steels which all exhibit yield strengths greater than 125,000 psi. These alloys were developed for their high elevated-temperature strengths and in general are more expensive and difficult to machine than is 17-4 PH.

From this perusal of potential ring-spring alloys, it is concluded that 17-4 PH has the best combination of yield strength, toughness, availability, cost and fabricability. This conclusion was partially influenced by the fact that 17-4 PH was the ring-spring reference material and other materials were not sufficiently better to warrant a change. If other springs are made in the future, however, a more thorough investigation of other alloys, such as the nickel-base type, should be made. There was

[3]DMIC Report 166, "The Effect of Nuclear Radiation on Structural Materials", Sept. 15, 1961, p. 38.

not enough time to completely investigate some alloy aspects, such as weldability, strength of casting versus wrought products, stress-corrosion susceptibility, radiation effects, etc.

D. Casting Vendor Talks and Processing Recommendations

The vendor who is producing the 17-4 PH ring-spring castings was visited and the results are reported.[4] Some of the recommendations from these talks include:

1) An improved inspection plan which details the radiographic and dye-penetrant inspection procedure.
2) The vendor was cautioned to not use halogen-containing solvents for cleaning and/or degreasing castings. Alcohol or acetone is a suitable substitute.
3) In view of the potential stress-corrosion problem with 17-4 PH the heat treatment was changed from H 900 to H 1025. Although this heat treatment reduces the yield strength from $\approx$180,000 to $\approx$160,000 psi, the material is more resistant to stress-corrosion cracking. The castings can also be machined after final aging with this heat treatment. Previously the castings were to be machined after solution heat treating but before aging at 900°F, and a shrinkage of $\approx$0.1% would be encountered. With the recommended sequence, there will be no dimensional changes after final machining.
4) Careful handling of the finished casting should be observed to prevent the formation of dents which act as stress risers. Identification of the casting by stamping of serial numbers in the surface is, of course, undesirable.
5) Avoid descaling in the 10% HNO_3 and 2% HF solution suggested in the brochure. Descaling in this manner would allow pickup of undesirable HF.

E. Casting Examination and Heat Treating Study

A representative casting in the solution heat-treated condition was metallographically examined to determine whether shrinkage porosity or dendritic segregation were problems. Several sections from critical portions of the casting were examined and it was noted (in eight samples) that the shrinkage porosity was less than 1% in all instances. Cored dendrites (a phenomenon where alloy-rich metal is surrounded by alloy-poor metal - this situation can degrade corrosion resistance and the response of the alloy to heat treatment) were noted in all samples, however, and a small heat treating experiment was performed in an attempt to dissolve, or homogenize, these alloy-rich areas with the matrix alloy.

For the homogenizing study, several casting sections were obtained adjacent to each other to minimize sample differences. Some sections were then re-solution heat treated at 1925°F (to provide a double solution anneal) and other sections were heated to the homogenizing temperature of 2100°F. These latter sections were also solution heat treated and aged at 1050°F to provide comparison samples with the double-solutionized

[4] See Memo of G. P. Fritzke to R. Miksch, "Trip Report to Perfecto Cast Co., San Diego, Oct. 26, 1965", November 1, 1965.

samples. The originally observed dendritic segregation was not appreciably affected by any of these heat treatments and therefore it would not be practical to attempt to homogenize the castings.

It is desirable to perform tensile tests on bars poured with each heat, however, to determine the change, if any, in expected mechanical properties due to this dendritic segregation.

III. SUMMARY AND CONCLUSIONS

From this study, several general conclusions can be drawn.

1) 17-4 PH stainless steel, heat treated to H 1025 is a suitable material for the ring-spring portion of the quick-disconnect vacuum coupling.
2) Corrosive environments, as they are now visualized, should not affect the ring-spring adversely. However, because of the great effect that minor variations in material quality, corrosion environment, etc., can have on the material, ring-spring failures will probably occur during accelerator lifetime. The cause of these failures will probably be due to some of the corrosion mechanism(s) discussed above.
3) No halogen-containing solvents should be used on the castings at any stage of processing.
4) The ring springs can be repair welded, but each instance should be handled as a special case to insure proper post-weld heat treatment, radiographic notation of weld area, careful visual inspection of the area, etc.
5) Materials with properties comparable to 17-4 PH can be used for the ring-spring material. However, no other material exhibits outstandingly greater reliability under BSY conditions.
6) A review of the casting vendor's facilities revealed good practices and a thorough understanding of our problem.

IV. RECOMMENDATIONS

In view of the critical role that the ring spring plays in the BSY design, several recommendations should be considered:

1) The present design calls for both male and female spring sections to be equally stressed. Since the male ring is "captured" by a flange and is difficult to replace, it is suggested that in a future design the male ring be overdesigned so the major stress load be carried by the female ring which is more easily replaced.
2) Several dummy assemblies should be positioned in the BSY where they can be subjected to conditions experienced by those in service. The dummies could then be examined periodically for possible failures and the results applied to those in service. As a part of this setup, it would be good to provide similar dummies in an area not subjected to the electron-beam effects so that samples could be periodically taken which would be divorced from beam effects. The assemblies in the BSY may become sufficiently radioactive to require hot-cell examination, whereas the "cold" dummies could be examined by conventional techniques.

ECL 59-F
Exhibit 2

Exhibit 2: Defective Ring Spring Casting. Arrow Indicates Crack.